The WELDING JOURNAL

MARCH 1951

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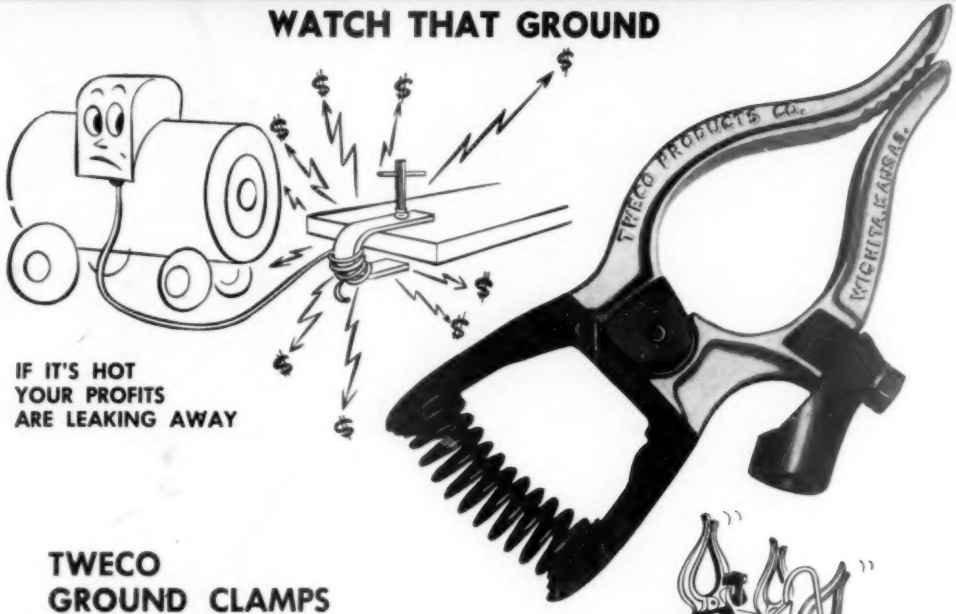

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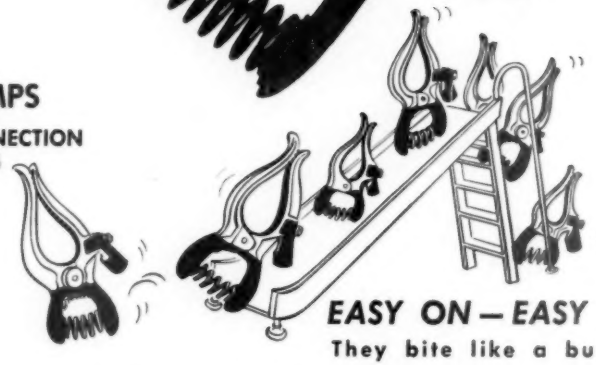

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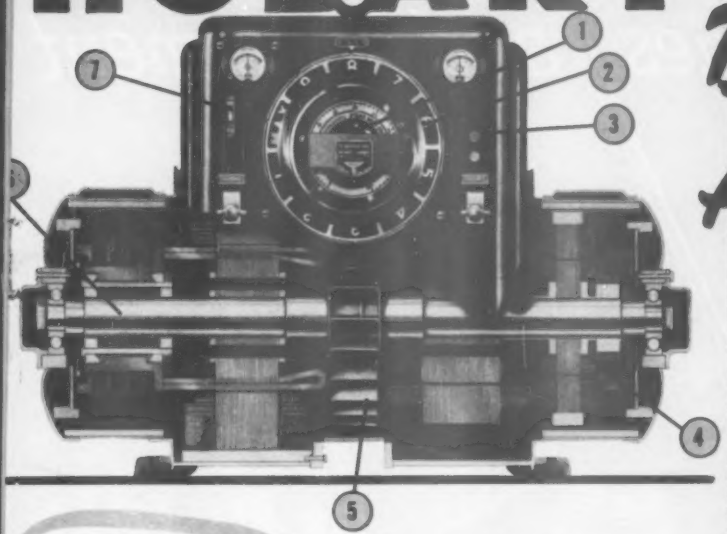

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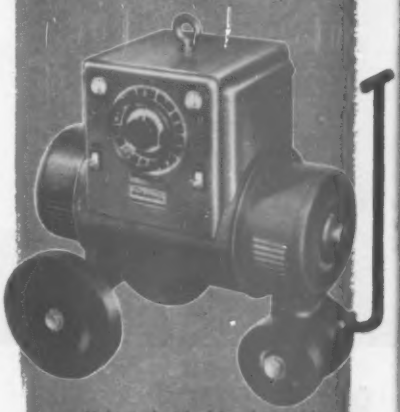

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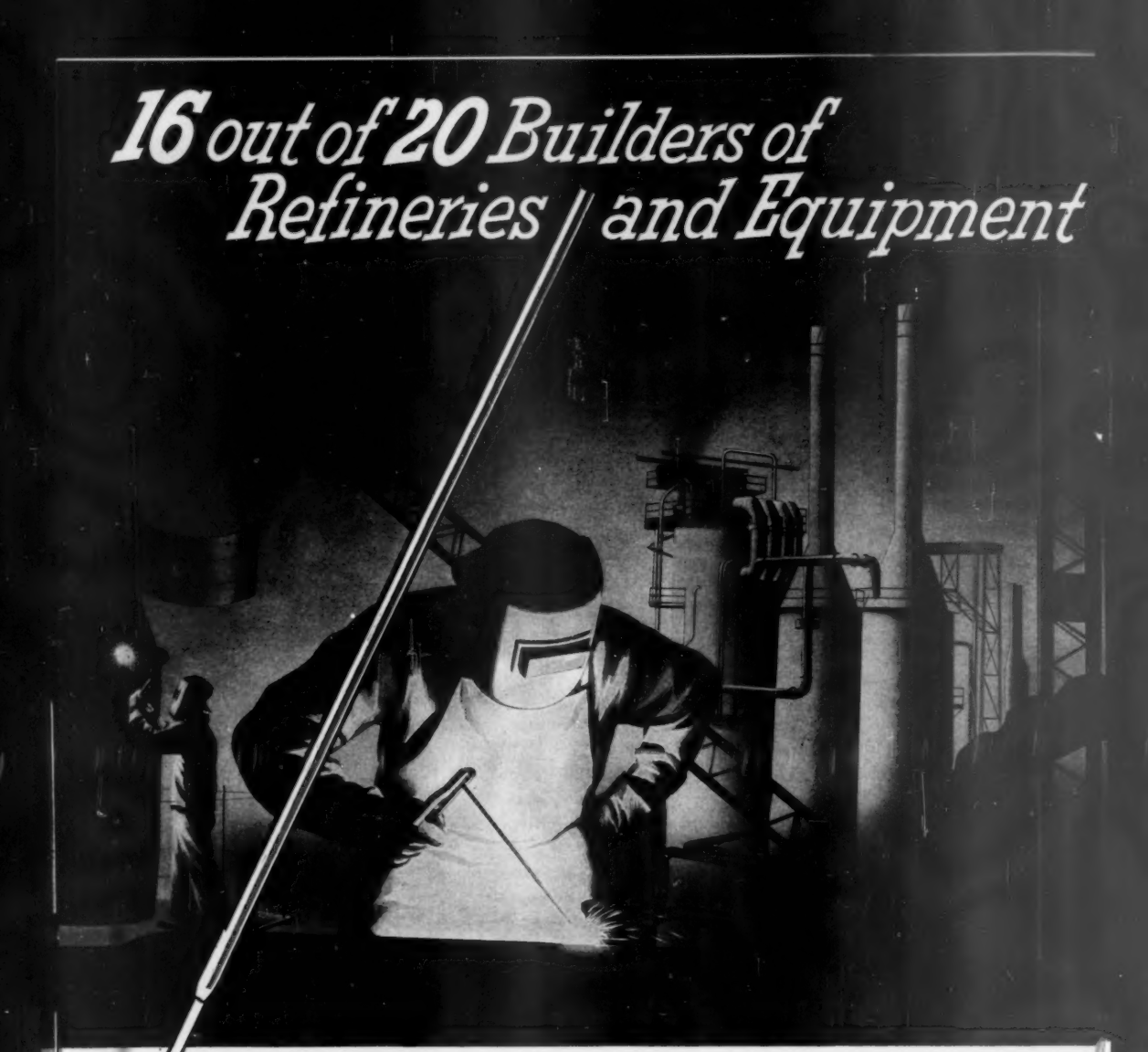

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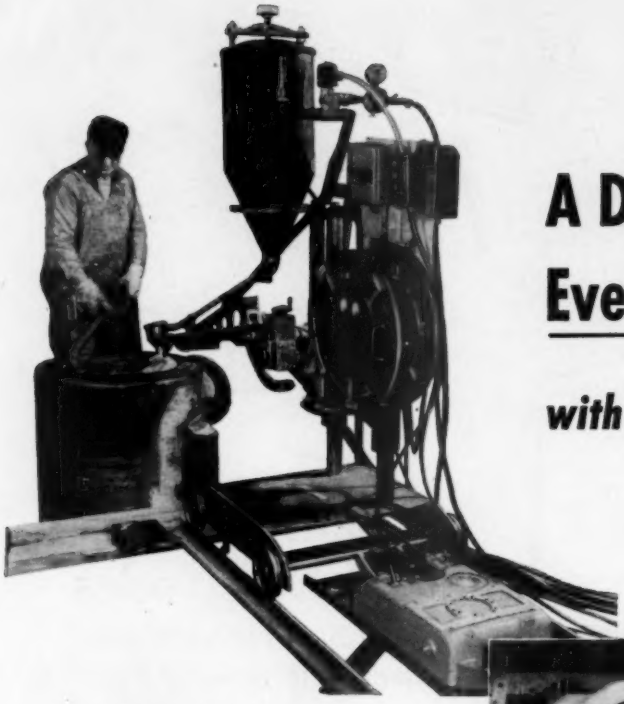

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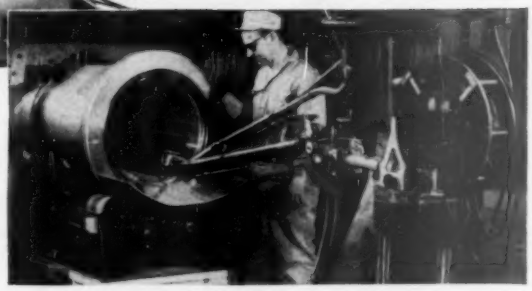

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The WELDING JOURNAL

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Edge Preparation of Plate for Welding

Flame shaping of plate edges for production welding to assure uniformity of cut surfaces and correct alignment

by C. A. Heffernon

WHEN plate is prepared for production welding it is desirable to use various types of edge preparation. The type of edge preparation varies with the thickness and type of material, method of welding, type of joint and angle of adjoining plates. The edge profiles shown in Fig. 1 illustrate the types of edge preparation which are most frequently encountered. The profiles from left to right progress from the simple square edge to the more complex double bevel and nose and the J-groove. The principal methods of producing these surfaces employ various types of oxygen-cutting equipment.

In order to assure uniformity of the cut surface, it is necessary to use either machine-carried cutting apparatus, which travels along a straight-line track or accurately follows a prescribed path, or a fixed apparatus

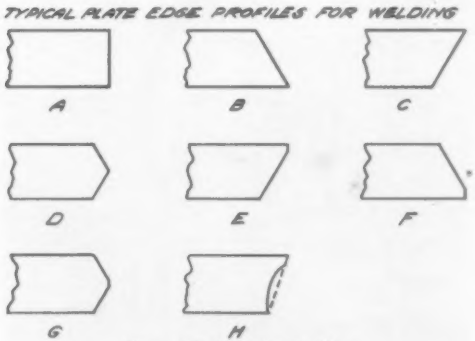

Fig. 1 Typical edge profiles

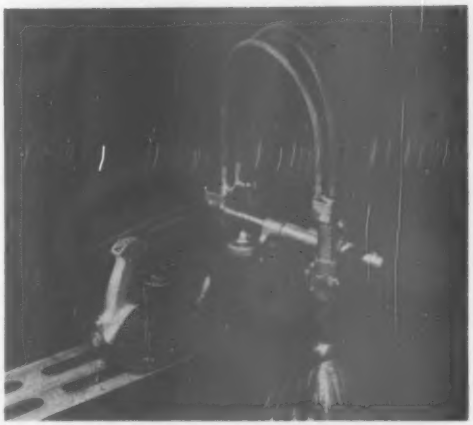

Fig. 2 Portable cutting machine

with the material moved in a suitable manner. In general, large-area plates arrive at the oxygen-cutting bed in varying degrees of flatness. This condition of the material has relatively little effect on the production of straight cuts with square edges, but has a very important effect on the uniformity of a beveled cut produced by a machine following a straight line. In the case of all beveled cuts, it is necessary to support the blowpipe or nozzle on a roller arrangement which contacts the plate as close as possible to the reaction zone. The vertical location or clearance of the nozzle above the plate surface is therefore controlled by the floating mechanism which rides the plate at a fixed distance from the machine as it moves along the track,

One of the more elementary types of edge preparation equipment is shown in Fig. 2. This illustrates a portable cutting machine with a single blowpipe and nozzle located in a position to produce a square cut at the edge of the plate. This equipment also can be used for pro-

C. A. Heffernon is Process Service Supervisor, The Linde Air Products Co., Indianapolis, Ind.

Presented at the Thirty-first Annual Meeting, A.W.S., Chicago, Ill., week of Oct. 22, 1950.

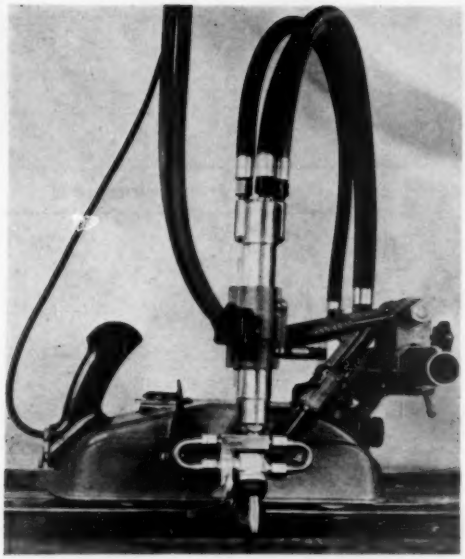

Fig. 3 Portable cutting machine, plate riding device and bevel cutting attachment

ducing beveled cuts on short sections of plate by tilting the blowpipe at the desired bevel angle or by using a bevel cutting adaptor in a vertically positioned blowpipe. However, this setup should be provided with a plate-riding device whenever long cuts of close accuracy are required.

The apparatus shown in Fig. 3 includes a portable cutting machine with a plate-riding device and a blowpipe with adjustable bevel-cutting adaptor or head into which the cutting nozzle is inserted. The plate-riding device has a roller which contacts the plate surface adjacent to the cut and the attachment pivots in a vertical plane about the horizontal axle or arm mounted on the top of the cutting machine housing. This relatively simple equipment also can be used for producing edge profiles having two planes by mounting a second blowpipe in the normal position on the machine. With this arrangement, edge profiles consisting of bevel and nose or double bevels can be produced by adjusting the nozzles to the planes of the desired edge profile. For greater accuracy of edge profile the beveled surface should be produced by the nozzle supported on the plate-riding device.

In the event that a double bevel and nose is to be produced, it will be necessary to use a nozzle arrangement capable of handling three or more nozzles in an adjustable cluster. The equipment shown in Fig. 4 includes such an arrangement. It consists of a heavy-duty cutting machine with plate-edge preparation attachment, screw adjustable nozzle block, and caster-type follower wheel with four nozzles located at proper angles with respect to each other and the plate surface. In this case the first nozzle is an auxiliary cutting nozzle which produces a leading under-bevel cut in the scrap material. Bevel cutting requires more preheat than square cutting because of the lower angle of impingement of the flames on the plate surface and the greater length of reaction zone as the cut penetrates at an angle through a given thickness of plate.

The second nozzle produces the desired under-bevel plane of the edge profile. It is sidewardly displaced from the lead nozzle by approximately $^1/_4$ in. and is 2 in. behind the lead nozzle so that the second reaction zone is carried through material preheated by the leading reaction zone. The cut surface produced by the leading nozzle is unimportant and may be rough as in the case of cutting at high speed with insufficient preheating, but the lower surface of the second cut must be smooth and uniform. The third nozzle in the group, also cutting through heated material, produces a square cut. The lower part of this cut becomes the nose of the double bevel and nose profile.

The thin section of metal left between the first and second cuts is easily penetrated by the square cutting nozzle reaction zone so that the slag from the latter jumps the gaps and is exhausted through the bottom of the plate. The top surface of this edge profile is produced by the fourth nozzle which cuts through solid material from the top side of the plate and exhausts through the preheated scrap material. The beveling angles of the nozzles can be accurately adjusted by rotating the nozzle holders in the squeeze clamps. The sideward displacement of the nozzles is accurately controlled by the adjustment of the cross feed screws. The distance, along the line of cut, between the beveling nozzles and the squaring nozzle should be adjusted to a minimum. This will bring the nozzles close to the plate-riding wheel to assure accuracy of the beveling reaction zones. However, the amount and tenacity of the slag adhering to the cut edges must be considered. To control the slag the distance between nozzles some-

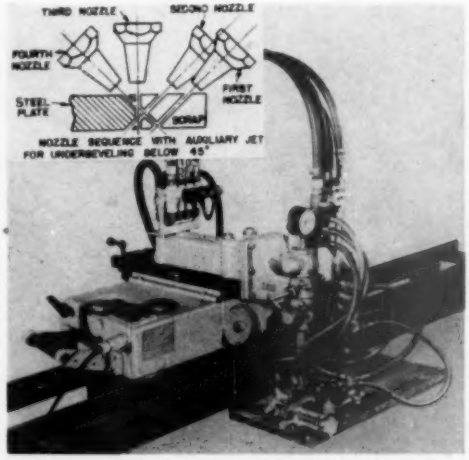

Fig. 4 Heavy-duty cutting machine and multiple face edge profiling attachment

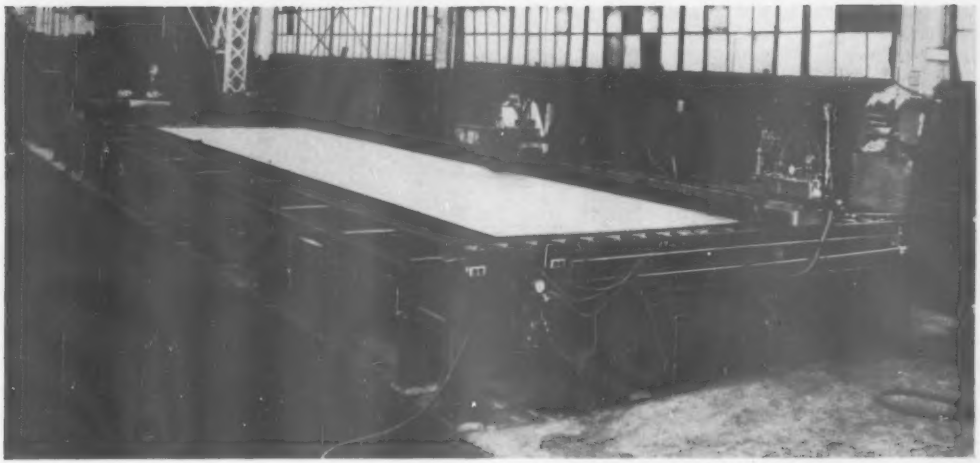

Fig. 5 "L" bed setup

times may be increased. This will help keep the material below the critical temperature at which the slag tends to fuse into the underlying kerf surface.

As the cut progresses along a prescribed line, the material along the length of the cut is heated by the reaction zone. A piece of steel that is heated along one edge tends to bow due to the expansion of the heated material. The bowing tendency is resisted by the mass and stiffness of the material between the heated edge and the opposite side of the steel section. In the case of short or small sections, the bowing is negligible, but becomes more pronounced in the case of long narrow sections. For example, cutting along a straight track on one side of a $^1/_2$-in. by 7-ft. by 37-ft. plate will cause the trimmed side to bow outward or convex about $^1/_{16}$ in. at the midsection. As this section plate edge cools to room temperature, the heated material, which was compressed or upset by the resistance of the remainder of the plate, shrinks to less than the original length. This causes the material along the edge to become stressed in tension and the plate takes a set with an inward or concave bow of about $^1/_{16}$ in. While a plate 37 ft. long with only $^1/_{16}$ in. out-of-straightness along one edge would appear to be straight enough, two such plates edge to edge will have a $^1/_8$-in. gap along the midsection. This means that weld materials and time will be wasted in filling in the gap during welding.

This condition is very much greater in the case of narrower plates. A $^7/_{16}$-in. by 30 in. by 30 ft. plate, cut along a straight track on one side, will bow outward $^{13}/_{16}$ in. as it is cut and will cool down to an inward deformation of similar amount. Thinner material bows more than heavier material. A 1-in. plate, 12 ft. wide and 20 ft. long, could be cut one side at a time with less danger of deformation due to cutting than a 1-in. plate 6 ft. wide and 20 ft. long.

Fig. 6 "L" bed setup

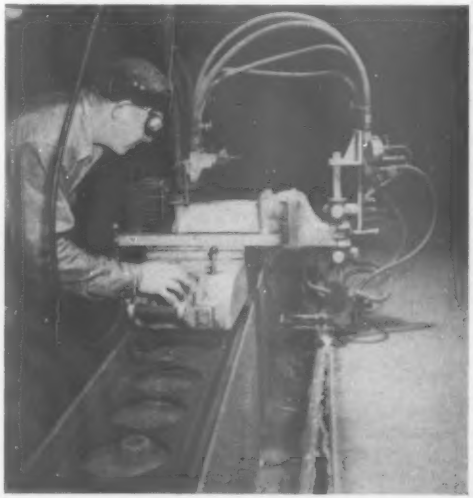

Fig. 7 Closeup of double bevel and nose operation $^1/_4$-in. plate. One side of "L" bed

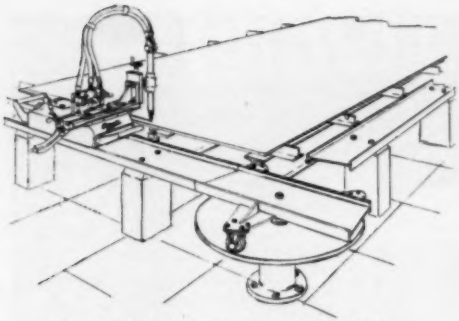

Fig. 8 "L" bed setup. Turntable sketch

Whenever excessive bowing is anticipated it is well to consider cutting the opposite sides simultaneously. This assures introduction of equal amount of heat on each side. Also the difficulty experienced in lining up one section of portable machine track ahead of another has led to the use of permanent track and material support installations in plants where repetitive edge preparation is to be carried out on a production basis. Permanent installations such as the "L" bed and flame planer type of plate edge preparation setup are described in the following section.

The assembly of large weldments such as pressure vessels and liquid holding tanks requires an extremely accurate control of the edge profile throughout the length of the prepared edges. In order to speed up production, it may be desirable to permanently fix two sets of tracks at exactly 90 deg. to each other. Figure 5 shows an "L" bed track which is used in preparing plates for assembly in large gas holders. This arrangement may also be used for tank production. With this arrangement, cuts can be made on adjacent sides of the plate simultaneously. After these two cuts have been completed, it is necessary to reposition the plate only once to complete the preparation of the four edges. This arrangement is suitable for large rectangular sections of plate.

Another "L" bed setup is shown in Fig. 6. This installation is capable of handling plates up to 32 ft. by 12 ft. The ball caster topped work support posts facilitate positioning the plates and the frame of angle iron provides a fixed base to which the plates may be securely clamped to prevent movement during edge preparation. A closeup of one of the machines in this "L" bed setup (Fig. 7) shows the double bevel and nose edge preparation on a section of $^3/_4$-in. plate. Note the normal separation of the scrap material from the plate section and the loosely adhering slag strips which separate with the scrap metal or may be easily scraped off in cleaning up the cut surfaces. The ease with which slag can be removed is a result of proper selection of nozzle size, nozzle spacing, preheat flow and cutting speed. A single machine may be used on an "L" bed setup by employing a turntable at the corner, as shown in the sketch, Fig. 8.

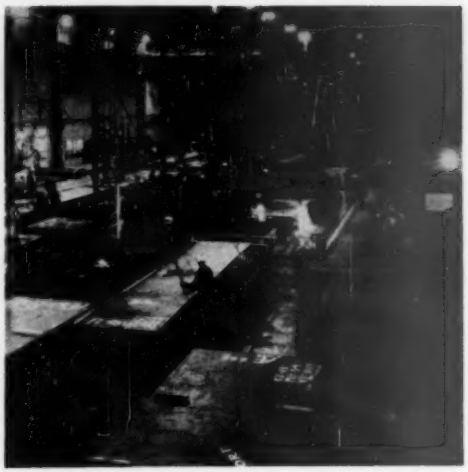

Fig. 9 Flame planers in shipyard (Richmond Yard No. 2)

The most accurate plate edge preparation setup and the one best suited for high-production rates is that known as a "flame planer." With this type of equipment the opposite sides of the plate are processed simultaneously and the ends may be cut at the same time or separately, depending upon the completeness of the flame planer setup. The complete flame planer consists of three carriages or gantry units extending across the cutting bed and riding upon a continuous pair of accurately machined and located tracks or rails. The center carriage makes the two side cuts simultaneously and the end sections are cut by machines which travel across the first and third gantries. The end cut operations are arranged so that there will be no interference of carriages and their timing will depend upon the size and shape of the plate sections.

The rate of production of a flame planer is not only a function of the cutting speed but is also dependent upon the loading and unloading rate of the cutting table. In order to facilitate loading and removal of the plate and to avoid delays due to interrupted crane service, a roller-type of cutting table is employed. Figure 9 shows an over-all picture of the large plate cutting installation in a shipyard during a period of high-rate ship production. (Kaiser, Inc., Richmond Yard No. 2, Richmond, Calif.) In this picture there are three flame planer lines of which the one in the foreground represents the latest type. Material for this roller table is loaded on the far end of the table and unloaded from the near end after the sections have been cut to size with the required edge preparation. The supporting frames of the cutting bed are separated from the loading and unloading ends to avoid jarring the cutting bed and to permit loading and unloading while cutting.

A straight-line, gantry-type plate-edge preparation unit or "flame planer" was made by the Mosher Steel Co. of Dallas, Tex., to meet their own requirements of

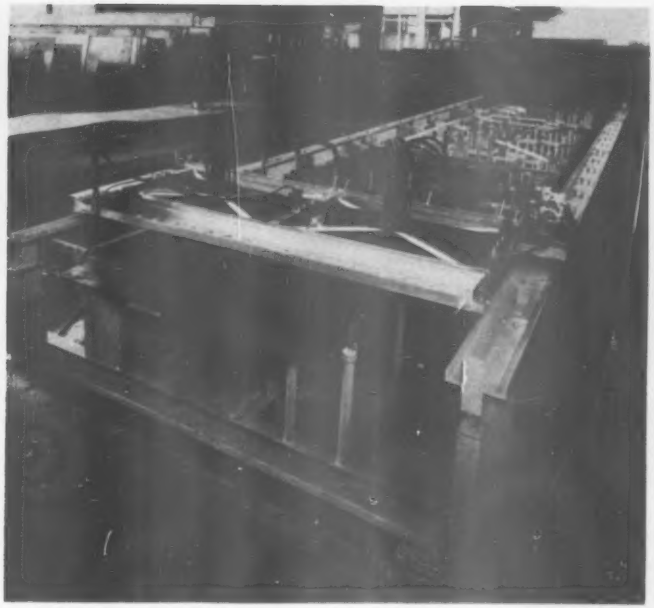

Fig. 10 Home-made flame planer. Cross-cut side

low cost, machine location, material handling facilities and plate sizing operations. This unit is shown in Fig. 10. The gantry carriage spans the work area and travels on parallel tracks. On one side there is a monorail and on the opposite side a standard double track for standard cutting machines is used. The tracks extend the full length of the work table and are mounted true and rigidly to the work support assembly.

The gantry carriage is constructed of standard structural steel members with tubular cross-bracing. The carriage is motivated by a standard cutting machine, which is built into and supports the carriage at one corner. The other three corners of the carriage are supported on roller- or ball-bearing mounted wheels riding on the inner tracks. The cutting machine drive wheel is rigidly connected through a 1-in. shaft to the drive wheel on the monorail side also.

A number of cutting blowpipes are mounted on a $1^{3}/_{8}$-in. O.D. cross bar which extends across the entire width of the carriage. The cross bar is equipped with a continuous 24 pitch $^{1}/_{4}$- by $^{1}/_{4}$-in. gear rack which facilitates adjustment of blowpipe spacing. The blowpipes are held in standard blowpipe holders mounted directly on the cross bar or connected through plate riding brackets. All blowpipes may be used for squaring cuts or plate-slitting but only the blowpipes in the plate-riding brackets can be used for beveling, in which case bevel-cutting adaptors are inserted between the nozzles and the blowpipes.

Square or bevel cross-cuts are made by a portable cutting machine mounted on a track extending across the front side of the carriage. The gas supply hoses are restrained in a hose trough which keeps them off the floor and reduces drag on the machine. Gas regulators and control valves are mounted on the machine to facilitate operator control. A number of swinging arms locate the workpiece from the monorail side of the setup. There are many other work-saving features in this unit which is suitable for limited plate-edge preparation work. Square cuts, straight bevels, single bevel with nose and limited double beveling can be performed accurately to meet

Fig. 11 Front view. Home-made flame planer. Newport News Shipbuilding & Drydock Co.

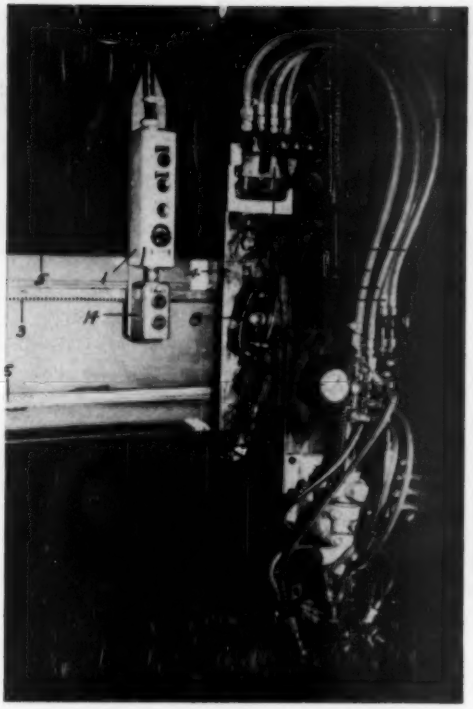

Fig. 12 Cutting unit, home-made flame planer. Newport News Shipbuilding & Drydock Co.

the ordinary requirements of this mill.

A special flame planer was built by the Newport News Shipbuilding and Dry Dock Co., Newport News, Va., to meet the requirements for edge preparation on extra wide plates. In addition to the greater distance between carriage wheels, this design also incorporated a different method of carriage wheel arrangement and a different type of clutch from that previously employed in other standard or special flame planers. This flame planer is capable of producing parallel edges on plates 21 in. to 16 ft. 4 in. in width. The over-all view of this setup is shown in Fig. 11.

The tracks are 126 ft. long and the material supporting skid extends for 120 ft. The bridge carriage movement is powered by a $^1/_2$ hp., d.-c. motor with Thymotrol speed control and magnetic clutch. This gives steady, dependable operation at any speed between 3 and 30 in. per minute.

The power is delivered through an endless chain and sprocket to the driving pinion which engages a rack extending the length of this track. The carriage is held square with respect to the rail by the two rollers at each end of the carriage housing. Perfect alignment of the rails can be maintained through adjustment of the rail support units which are spaced on 3 ft. centers. The rack and pinion engagement on both rails assures that the 20-ft. wide carriage remains square with respect to the rails as it moves forward or backward.

Figure 12 is a closeup of the cutting equipment showing the standard screw adjustable nozzle block; the roller bearing, vertical floating member or post; gas distribution manifolds; mixer; valves; and the electrical control buttons for magnetic clutch and speed control. The small crank arrangement shown in the center of the picture contains a planetary gear system for moving the cutting equipment sidewards along the carriage. It is possible to use this arrangement in making fairing cuts. At the present time the end cuts on this setup are handled by portable cutting equipment although the carriage design is such that a cross-cutting unit could be mounted on the carriage at a later date if so desired.

Another gantry-type plate edge preparation unit was prepared by Gilmore Fabricators of Oakland, Calif., using shape-cutting machine tracks and wheel assemblies and standard cutting machine units as the motivating elements. An over-all view of this setup is shown in Fig. 13 with three vertical blowpipes individually supported by plate riding attachments, splitting a long $^1/_2$-in. plate into two uniform width sections at 26 in. per minute cutting speed. The gantry carriage rides on an inverted "V" section at the right side and a flat rail at the left side. The operator is cutting up the scrap trimmed from the edge of the original plate as the longitudinal cuts progress.

A closeup of the right side of the gantry carriage showing the plate riding units rotated upwards out of the way; the quick-acting shutoff valves for preheat and cutting oxygen; and the details of the carriage drive and wheel assembly may be seen in Fig. 14. The

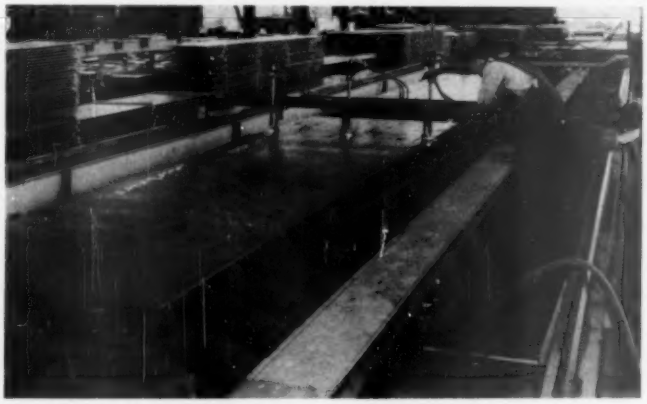

Fig. 13 Gantry-type unit Gilmore fabricators, over-all view

Fig. 14 Gantry-type unit, carriage drive and wheel assembly

driving wheels of the standard cutting machine unit contact a narrow runway of light steel plate attached to the right side of the cutting bed. The cutting bed consists of bars of light steel plate of uniform width held in vertical position by the brackets of angle iron sections welded to the foundation structure.

The cross-cutting carriage unit is independent of the main gantry carriage but rides on the same rails with similar wheel assemblies as shown in Fig. 15. Square or bevel cross cuts can be made with the portable cutting machine which rides on a straight track extending across the inverted channel member of the gantry carriage.

A plate-slitting unit has been prepared by the Alabama Dry Dock and Shipbuilding Co., Mobile, Ala., using a standard flame planer as a base and adding a home-made arrangement for supporting several nozzles on the leading side of the gantry carriage as shown in Figs. 16 and 17. This unit employs one or two plate riding columns, depending upon the number and spacing of the nozzles. The nozzles are supported by nozzle head holders which are adjustable along the cross bar.

In Fig. 16, six nozzles are cutting two parallel $^3/_4$-in. thick scrap plates into four 8-in. wide stiffener bars. The nozzle height is controlled by the single plate-riding unit which rides on the surface of the right-hand plate. Figure 17 shows a seven-nozzle setup for slitting $^1/_2$-in. thick scrap plates into 13-in. by 30-ft. bars.

As many as five 36-ft. long, 18-in. wide bars have been cut at one time from scrap $^1/_2$-in. thick plate at a cost considerably lower than the previous method of portable machine-cutting. It is interesting to note that this temporarily idle flame planer is again busy turning scrap plate into useful stiffener sections, gunwale bars and other parts, for ship repair work at a cost of 25 to 33% of the cost of new material. Current operating cost includes scrap value of the plate, labor and material costs.

Another strip cutting setup has been made by the Marion Power Shovel Co., Marion, Ohio, to cut and trim handle assembly members up to 40 ft. long while holding to accurate measurements of width and longitudinal straightness. Simultaneous cutting with two blowpipes mounted on the floating arm supported from the standard cutting machine prevents bowing of the narrow members used as large handle top and bottom bars.

In connection with the assembly of pressure vessels and liquid storage tanks, a large number of dished heads

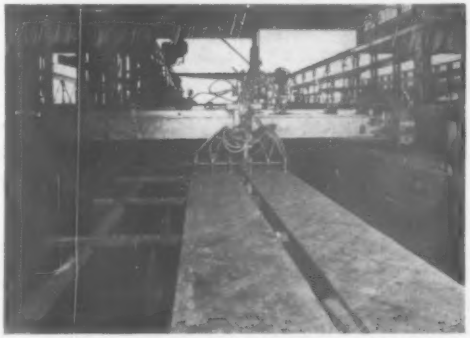

Fig. 16 Plate slitting unit. Alabama Drydock & Shipbuilding

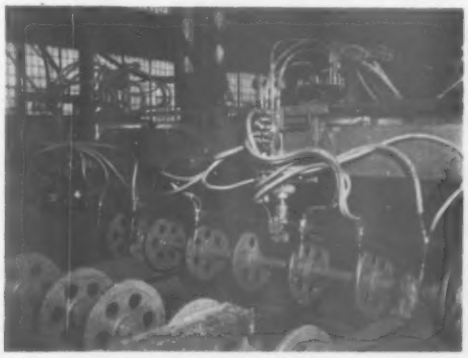

Fig. 17 Closeup, plate slitting unit. Alabama Drydock & Shipbuilding

Fig. 15 Gantry-type unit, cross-cut carriage

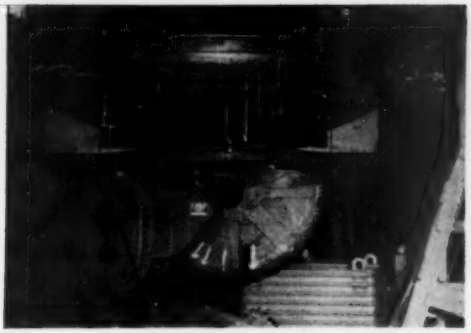

Fig. 18 Dished head edge preparation unit

are used. These heads vary in size, shape and diameter to meet design requirements. The welding requirements involve normal edge preparation specifications, and there have been several installations made to incorporate standard plate-edge preparation equipment for this purpose. The illustration in Fig. 18 shows a dished head mounted on a standard welding positioner in such a manner that the edge of the head may be rotated past a standard plate edge preparation unit. This unit has a spring-loaded, roller-bearing guided column mounted in a horizontal position with plate-riding wheel contacting the dished head so that a bevel and nose edge profile can be formed around the edge of the head.

Other installations for oxyacetylene edge preparation of heads include the use of portable machines operating around a pivot post located within the head and special plate edge preparation units mounted on centrally located pivot arms.

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(*Continued on page 256*)

Commercial Flame Hardening

Flame hardening requires suitable equipment and design. The proper combinations of gasses, equipment and methods are important

by Elmer J. Cox

THE proving ground for all new heat-treating methods and equipment is in the plants of the Commercial heat treaters. This is especially true of flame hardening. Some commercial plants have given flame hardening a black eye in the minds of some people due to their lack of proper equipment for producing uniform results.

As the demand for flame hardening increased, the Pittsburgh Commercial Heat Treating Co. decided to enter the field. However, it was decided as a company policy that all flame-hardening operations would have to be mechanized and that all work performed would have to be up to the high standards which the company has set for 28 years.

There are two main problems facing modern well-equipped commercial flame-hardening companies. The first is to overcome the apathy toward flame hardening that has appeared in some plants which have had unsatisfactory results due to lack of knowledge or lack of the proper equipment. The second problem is the lack of experimental opportunity. The customer sends in a part to be flame hardened, it may or may not be made of a proper grade of steel for flame hardening. It may or may not be of good design for flame hardening. Or, the analysis of the steel may not be known. In other words, the Commercial heat treater has one chance to turn out a good product with no experimenting and win an advocate for flame hardening. Thanks to experience and proper equipment this is usually the case.

The Commercial Flame Hardener has at his disposal various methods of heating. There are many variables which may affect his decision as to which method would be best for a given part. Not the least important of these variables is cost, which is sometimes critical. The other variables are type of steel, shape of part and most important is the service conditions under which the part must operate. For very severe service conditions such as mining machine gears which are subject to shock, a soaking heat is required which will harden to the root of the teeth. This is best obtained by using natural gas and air with appropriate mixing and burning equipment. For high-surface hardness and relatively thin cases, the use of oxyacetylene heating is required. Midway between these two extremes is a third choice of oxygen and natural or manufactured gas.

The various combinations of gases require various types of mixing equipment to insure proper combustion. Natural gas and air is mixed by a proportional mixer which delivers a constant air-gas mixture at a constant pressure to the burners. Oxyactylene and oxyfuel gases are fed at constant pressure, controlled by regulators, to mixing blowpipes which are designed to properly mix the gases and prevent flash backs of the burners.

A wide variety of burners must be on hand in a commercial flame-hardening department, in order to do a good job quickly and economically. Radiant burners are used for air and natural gas mixtures. These are mounted around the circumference of gears and the gear is rotated. For oxyacetylene there are two types of burners, those with integral quench and drilled flame ports for progressive hardening, and those with integral cooling with either drilled flame ports or screw in flame ports. All oxyacetylene burners must be cooled due to the very high temperature of the flame. Screw-in tip type burners are very versatile in that various lengths of tips may be used to conform to the contour of the part to be hardened.

There are generally considered to be four distinct types of Flame Hardening. They are spinning, progressive, combination and spot hardening. The spin method is used for parts that are not too large and are symmetrical. These parts such as spur gears, bevel gears, rollers and pins are spun with the oxyacetylene flames impinging on the surface to be hardened. When the part is up to the quenching temperature it is either dropped into the quenching medium or spray quenched after the flames are extinguished. Progressive hardening is just what the name implies either the flame head moves across the part or the part moves past the flame head. The flame head usually has an integral quench which follows immediately after the flames. In the combination method, the part is spun while the flames and quench move along its axis. The spot-heating

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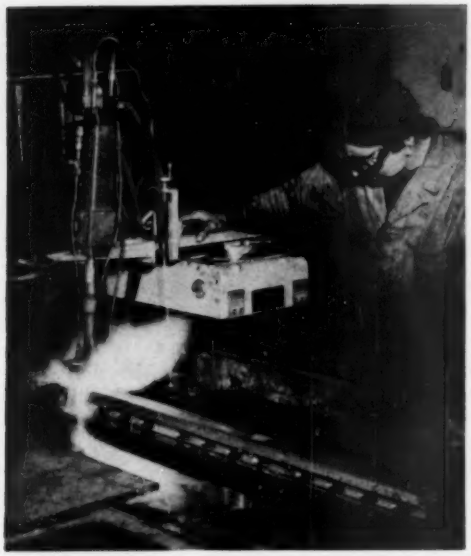

Fig. 1 Linde machine carriage being used to progressively harden a rail

method has very limited application and is merely the heating of small parts with a single flame and then immersion quenching them.

The type of quench used is very important. In the spin-hardening process, with immersion quench, either oil or water may be used depending entirely upon the type of steel and the hardness desired. In spray or progressive quenching, air, water or various mixtures of soluble oil may be used; again depending upon the type of steel and hardness desired. For all plain carbon steels in the 40/50 carbon range, water is the usual quenching medium. For the various alloy steels either water or soluble oil is used. For instance in the case of a part made from S.A.E. 4340, we used a heavy soluble oil quench 5 in. behind the flames and produced the required hardness of 75/85 shore.

The control of temperature is in the hands of skilled operators who judge the temperature visually to set up the heating time or progressive speed. Recently there

Fig. 2 Double helical mill pinion shaft being hardened by the progressive method

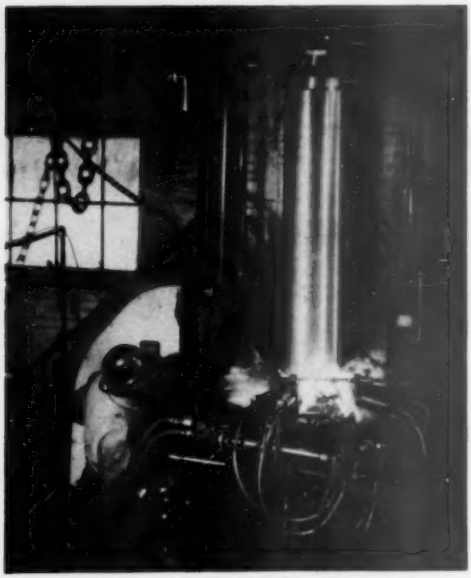

Fig. 3 Large roll being flame hardened by the progressive method

Fig. 4 Roller-bearing race being progressively hardened on a Denver Universal flame hardener

has been an adaptation of the radiation pyrometer to automatically control and limit cycles. This very important and progressive step was taken by the Cincinnati Milling Machine Co., when they incorporated a radiation pyrometer in their "Flamatic" flame-hardening machine.

Fig. 5 Herring-bone gear being progressively hardened on a Denver machine

an alloy steel such as S.A.E. 4140 heat treated before machining to 250/300 Bhn. should be used. Meehanite is a very good flame-hardening material if well-designed heating equipment is used and shallow hardness is required.

I will now describe some of the machines in our plant which are used to provide the various methods of heating and quenching. Probably the most versatile and indispensible machine in commerical flame hardening is the standard cutting-machine carriage. This machine may be equipped with various flame heads to harden parts such as gears, pinion racks, rails, locomotive slides, etc. The machine is driven by a variable speed motor which permits close control of case depth. Figure 1, shows this machine being used to harden a rail slide. It will be noted that the part is immersed in water to prevent warpage. One end of the rail is a rack and the flame speed must be increased over this area to prevent burning of the teeth. Figure 2, shows the same machine being used to harden the teeth of a mill pinion. For this operation a Linde thirty flame head is used to harden one side of a tooth at a time. The same machine is also used to harden the wobbler pads on this pinion shaft.

In commercial flame hardening there is a need for a machine to harden rolls and shafts. This need is filled by the Denver Universal Flame-Hardening Machine, which is adaptable to hardening of many divers parts. Figure 3, shows a roll 12 in. diameter and 66 in. long

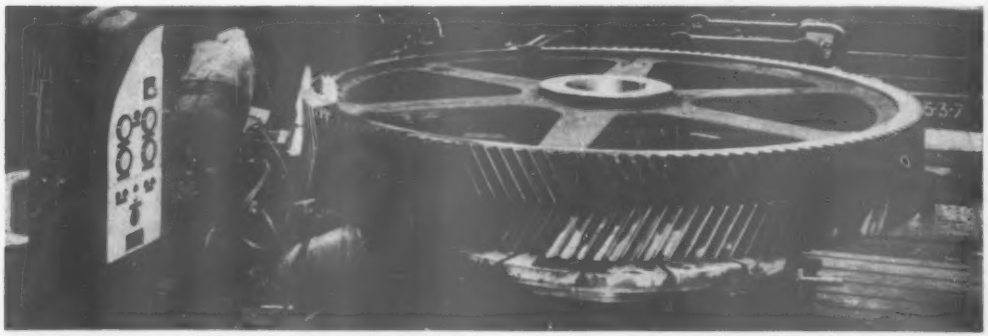

Fig. 6 One of the largest mill gears being progressively hardened

Case depth may be controlled in two ways. The first and most accurate is to set up a time cycle and then cut the part and polish and etch it to check the case depth. This method is most satisfactory for production runs. For smaller runs the case depth can be checked with a fair degree of accuracy by hardness readings. However the case depth on most parts must be calculated using time, speed and heat input.

With certain limitations almost any steel or iron that can be hardened is suitable for flame hardening. For most applications the 40/50 carbon steels are most satisfactory. They combine good wearing qualities with economy. For repeated shock or heavy loading

Fig. 7 Spur gear being spin hardened on a Cincinnati Flamatic

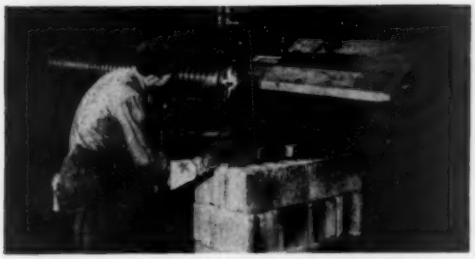

Fig. 8 Rolling mill hold-down screw being progressively flame hardened

being hardened by the combination method. The shaft is surrounded by flame heads which are immediately followed by quench heads. The roll is rotated at a surface speed of 1060 in. per minute and the flame and quench heads move up along the axis of the roll at a speed of $2^3/_4$ in. per minute. An air blast is used directly above the flame heads to cut off the secondary flame and reduce preheating of the roll. The combined gas consumption on this operation is in the order of 4000 cu. ft. per hour. Figure 4, gives an idea of the versatility of this machine. It shows a 9-ft. diameter roller-bearing race being hardened by the progressive method. The race is rotated past the flame heads at a surface speed of 3 in. per minute. This is only one of many various sizes and shapes of races and rings which are hardened on this machine. Figures 5 and 6, show large diameter double-helical gears being progressively hardened. The large gear in Fig. 6 is 150 in. diameter, 1 dp-20-in. face. Due to the extreme size of this gear, our machine was transported to our customer's plant and their table was used to turn the gear.

Probably the most advanced step taken in the field of flame hardening was the production of the Cincinnati Flamatic flame-hardening machine. This machine is built by one of the country's leading machine tool manufacturers, with the same precision qualities as their machine tools. It is the first flame-hardening machine with automatic electronic-temperature control. Once their machine is set up by a skilled operator it can thereafter be operated by unskilled labor. Figure 7, shows an example of spin flame hardening. The flame heads are mounted around the periphery of the gear and at a predetermined temperature, as indicated and recorded by a Leeds and Northrup Speedomax pyrometer, the flames are extinguished and the gear is automatically dropped into the oil quench. The parts are removed from the quench by a conveyor which may be seen in the picture. For parts that lend themselves to spin hardening this machine is the most accurate method yet devised. Some of the parts that are spin hardened are shown beside the machine.

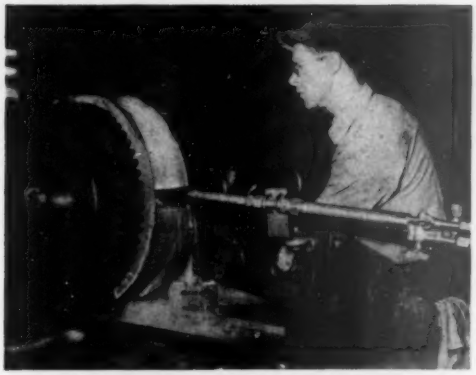

Fig. 10 Track wheel progressively flame hardened

Commercial flame hardening requires a lot of ingenuity in devising methods for hardening the odd and difficult parts. We have solved this problem, for the present, by redesigning a lathe and incorporating a variable speed drive and variable feed. Figure 8, shows this lathe being used to harden the screw thread on a rolling mill hold-down screw. The screw is made from S.A.E. 4340 which is a very difficult steel to progressively flame harden. However, this screw was hardened satisfactorily by using a delayed soluble oil quench. Figure 9, shows a Meehanite Crankshaft being spin flame hardened. A specially designed flame head is used to produce quick heating and the proper

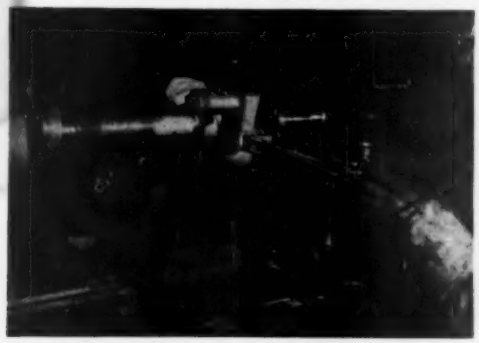

Fig. 9 Spin hardening a Meehanite crankshaft

Fig. 11 Motor gear being radiant heated

Fig. 12 Radiant gear-hardening machine

heat pattern. A soluble oil-spray quench follows the heating cycle. This method of heat treatment proved to be the only feasible method for hardening these crankshafts. Figure 10, shows a track wheel being hardened by the progressive method using a special flame head with an integral quench. The face and flanges of track wheels are hardened to give a very satisfactory increase in wear properties.

Figure 11, shows a gear being heated by radiant burners. This machine was developed by our company to meet the need for a relatively inexpensive gear which would have good wear properties and high resistance to shock loading. This method of heating produces a fully hardened tooth and rim, still leaving the hub and web of the gear in the normalized condition for good strength properties. At the end of the heating cycle, the gear is lowered into the quench. Figure 12, shows the base of the machine which contains the quenching medium which may be either oil or water depending upon the type of steel used.

We are at the present time installing a Gleason gear-hardening machine to progressively harden spur and bevel gears. This is being done in conformity with our premise that flame hardening must become more and more mechanized so that it may take its rightful place in the heat-treating field. This is the challenge then to Commercial flame hardeners and machine builders to mechanize flame hardening to produce uniform and high-quality products.

Magnaflux Inspection of Welded Storage Tanks

Hazards often encountered in magnetic inspection work can be avoided by the use of a high-quality "magnetic ink" instead of the dusting of powder

Discussion by R. Schnurmann

Magnetic flaw detection owing to its simplicity and rapidity has become an established inspection method in many industries. In his recent article on "Magnaflux Inspection of Welded Storage Tanks" Upson* described suitable methods of magnetizing sections of the welded tanks for inspection work by the magnetic method.

For the inspection of welded tanks, and quite generally for the successful application of the magnetic method, the essential requirement is to use a reliable indicator to show up either internal flaws or surface cracks. This is the direction in which considerable progress has been made in recent years.

Upson refers in his article to the difficulties experienced with powders which are available in gray, red and black colors. The gray Magnaflux powder, for instance, requires for its successful application favorable lighting conditions as it shows little contrast on a dark surface. With any powder excessive amounts tend to mask otherwise recognizable flaw patterns. The most serious types of defects to be found in a welded seam are elongated in a direction parallel to the seam; "accordingly, any surface irregularities similarly oriented will tend to mask, by simple mechanical entrapment of the powder, any Magnaflux indication of a defect." In humid weather, the red and black Magnaflux powders are said to appear to absorb moisture from the atmosphere to a certain extent, with the result that they show a tendency to agglomerate.

These are the hazards which it is essential to eliminate to make the magnetic inspection method fully dependable, and they are hazards which easily can be avoided by the use of "magnetic ink" instead of dusting a powder on to the magnetized specimen which is under test. It has been recognized for some time that it is advantageous for magnetic flaw detection to give the magnetic particles a high degree of mobility along the surface of the magnetized portion of any testpiece and for this reason attempts have been made in many places to produce "magnetic inks" to take the place of iron filings which are sprinkled on the surface of the testpiece or of magnetic powders such as the Magnaflux powders.

A good "magnetic ink" must allow of high mobility of the magnetic pigment in its vehicle combined with a comparatively high degree of stability of the suspension. The pigment must be free from any tendency to particle agglomeration so that in the absence of a

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* Upson, F. A., "Magnaflux Inspection of Welded Storage Tanks," THE WELDING JOURNAL, **29** (1), 27-30 (1950).

magnetic field slight agitation of the "ink" will achieve immediate uniform redistribution of the pigment.

Supramor Electromagnetic Flaw Detection Ink,† which is available in white, red and black colors each graded to suit any industrial job, combines with the mobility of the magnetic particles in suitable vehicles the unique advantage that it is free from caking trouble and that agglomeration of the particles is excluded so that the clarity of the flaw pattern is ensured and even minute surface cracks do not escape the inspection personnel's attention.

The properties of Supramor Electromagnetic Flaw Detection Ink, in particular its proneness of "going for" the fault, have proved, during its career in a wide variety of industries, that its use is very economical even when it is applied by either spraying the "ink" or pouring it from a nonmagnetic ladle. As regards the effect of humidity the experience was gained in shipyards, for example, that large ships' propeller shafts could be successfully examined in the yard even in rainy weather without requiring the otherwise appreciable expenditure of time and labor necessary for moving the shafts indoors for inspection purposes.

Author's Reply

Mr. Schnurmann suggests that gray Magnaflux powder requires something special in the nature of lighting conditions in order for its use to be successful. Actually it has been my experience that nothing particularly special is required except in the sense that some colors show up to better advantage on one background than on another. In this respect, the gray powder has no general advantage or disadvantage in comparison with powders of other colors. It is not clear to me how the "magnetic ink" would improve this situation to any appreciable extent unless it is available in gradations of color in which case special shades could be tailor-made to provide optimum contrast under specific conditions.

The greater mobility of the suspended magnetic pigments in the "magnetic ink" as compared to the mobility of the dry powders is unquestioned but it seems entirely possible to me that this very feature might make it extremely difficult to use the material on a vertical surface as is necessary in welded storage tank applications.

Although pure mechanical entrapment by surface irregularities might well be more objectionable when using dry powders than when using the "magnetic ink," there is also a magnetic bridging across any surface groove which might prove to be more troublesome with the "magnetic ink" than with the powder because of the former's obviously greater mobility.

One particularly serious disadvantage of any wet process technique in so far as welded tank applications, for instance, are concerned is that the vehicle for the magnetic pigments would have to be completely removed from any surface such as a backchipped welding groove before welding could proceed. Failure to do so would interfere with the production of a sound weld and, if the vehicle be flammable, would impose a safety hazard.

If the "magnetic ink" can be successfully used on wet surfaces, then in this particular it offers a very material advantage over the use of dry powders, and if the sensitivity and clarity of pattern are comparable to those obtained with the dry powders on a dry surface, there are undoubtedly many field applications for the material.

The use of dry powders has been developed to the extent that many types of subsurface defects are susceptible to more or less ready detection. It has been my understanding, however, that so-called "wet process" techniques have been comparatively unsuccessful in the detection of other than surface discontinuities (i.e., defects at or extremely near the surface). The extent to which the "magnetic ink" overcomes this limitation can, of course, be determined only on the basis of comparative tests under various conditions. I have also understood that "wet process" techniques are generally most satisfactory where the part being inspected can be completely immersed in the fluid.

In summary, without having any actual experience in the use of "magnetic ink," it would appear to me that the material might offer some advantages at least under specialized conditions such as where the surface being tested is approximately horizontal, where the suspected defects are likely to be substantially of the surface type and perhaps where total immersion of the part being inspected is practicable.

† Supramor Electromagnetic Flaw Detection Ink is a product of Manchester Oil Refinery Ltd., Manchester, England.

Welding Quality Control in a Petroleum Refinery

Welding application in a large petroleum and chemical refinery requires high-quality strength welds and careful procedure control, specifications and inspection

by A. F. Blumer

ABSTRACT

A large petroleum and chemical refinery is a complex system of processing equipment, piping and tankage. All of this must be maintained in safe and efficient operating condition and much of the maintenance requires welding. A large variety of welding applications is encountered. In the interest of obtaining high-quality strength welds and performing welding in the most efficient and economical manner, welding quality control must be exercised. Welding quality control consists of qualification testing of welding operators, continued sampling and welding inspection on jobs in progress, testing and selection of suitable electrodes, adherence to carefully developed welding procedures and job specifications and evaluation and use of applicable new developments in the field of welding.

INTRODUCTION

IN A discussion of welding quality control in a petroleum refinery, it is perhaps better to approach the discussion by first outlining the types of welding encountered in the refinery and over which quality control must be exercised.

The Baton Rouge Refinery was first put into operation in 1909, and since that time has developed into one of the largest petroleum and chemical refineries in the country. The refinery contains eight crude-distillation units, three catalytic-cracking units, three alkylation plants, two phenol-treating plants for lube oil manufacturing, several types of lube oil dewaxing plants and units for the manufacture of alcohols, synthetic rubbers and synthetic rubber raw materials. Associated with these units are many other process units for finishing the base stocks produced, for the production of certain of the feed stocks used and for the manufacture of other products such as asphalts and greases. Tankage must be provided for the feed stocks, intermediate stocks and finished stocks, and there are many millions of feet of piping connecting all of this equipment.

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Presented at the 1950 A.S.M.E. Petroleum Division Conference, New Orleans, Sept. 24-28, 1950.

Fig. 1 Field pipe welding

All of the process equipment, tankage and interconnecting piping must be periodically maintained and much of this maintenance will require welding. It can be appreciated that this will amount to a very large volume of welding.

At the present time, there are 96 welders not including the supervisory personnel, 88 of these are metallic arc welders and eight are acetylene welders. In addition to the refinery welding personnel, there are 13 welders employed by contractors performing some type of work in the refinery.

PIPE WELDING

Pipe welding probably constitutes the major portion of the welding operations in the refinery. Figure 1 illustrates welding being performed on a field-pipe installation. As much as possible, pipe work for the units is handled on a prefabricated basis. Figure 2 illustrates a typical shop fabrication job on a complex manifold.

While the vast majority of the pipe welding is performed on carbon-steel piping, some alloy pipe welding is also done. This type of welding, however, will be discussed more fully under "Alloy Welding."

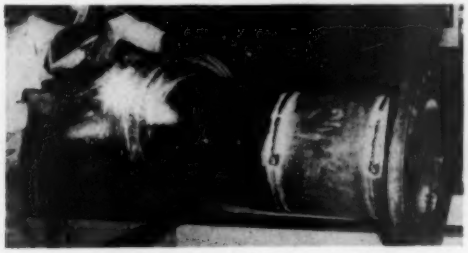

Fig. 2 Shop pipe fabrication

PLATE AND STRUCTURAL WELDING

Plate and structural welding probably constitute the second largest single phase of welding operations. Figure 3 illustrates alterations being made to a pressure vessel in the shop. Figure 4 shows a shell of a heat exchanger being fabricated in the shop. Figure 5 shows the installation of a 24-in. manhole in a pressure vessel in the field. In this particular installation, the

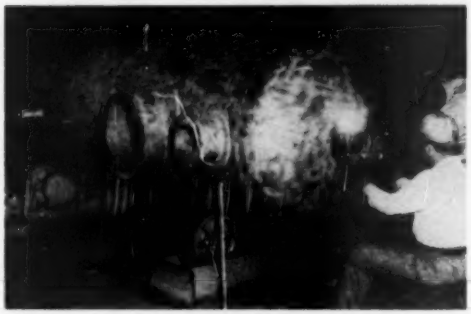

Fig. 3 Altering pressure vessel

Fig. 4 Fabrication of exchanger shell

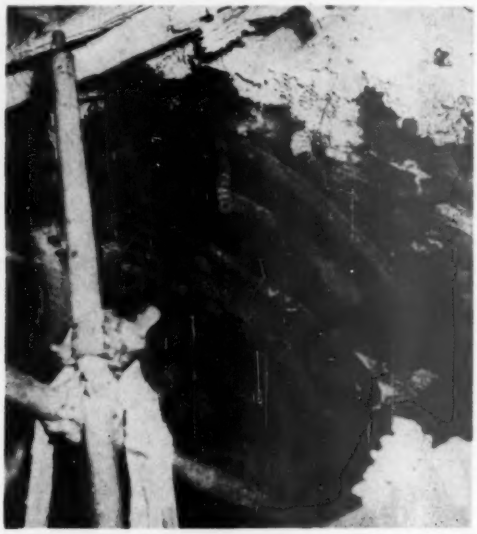

Fig. 5 Installation of 24-in. manhole

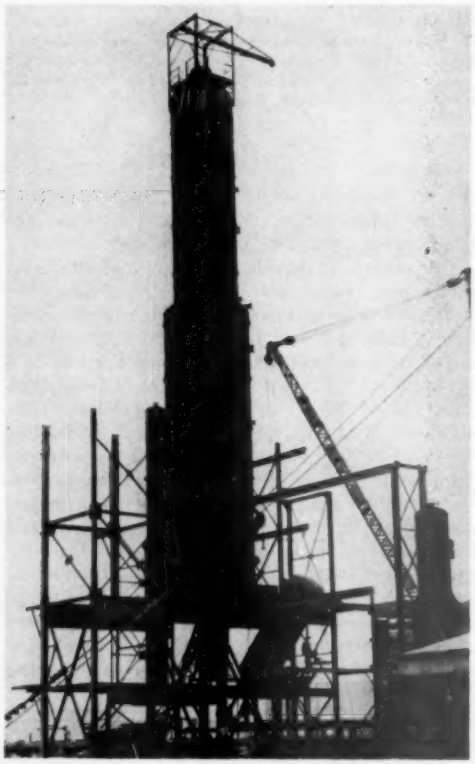

Fig. 6 Field-fabricated tower

plate thicknesses were such that stress relieving was required. This thermal stress relieving was performed in the field by the use of a gas burner ring, and it was at this stage of the work that the picture was actually taken.

On some of the process units, the vessels are too large for shipment by rail and in these cases they are fabricated in the field. This particular type of work may be handled by contractors' welders as well as by refinery welders. Figure 6 shows a 16 ft. 6 in. diameter by 125-ft. high tower which was field fabricated.

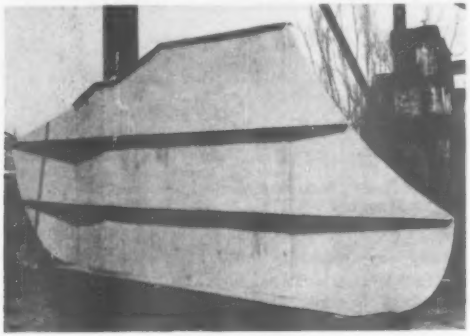

Fig. 7 Ship's rudder

Structural welding also constitutes a large phase of welding operations. In this particular field, supports and platforms are included. In some cases, the structural steel of building frames is also welded. Figure 7 illustrates a welding operation which would not normally be associated with a petroleum refinery. This figure shows the construction of a rudder for one of the river tow boats. On this job, the steel plates and structural members were precut in the shop and the pieces assembled at the river.

STORAGE VESSELS

Another phase of welding which constitutes a rather large field is the erection and maintenance of atmospheric and pressure-storage vessels. Figure 8 shows repairs being effected on a storage tank. On this tank, the top shell ring, internal structural steel and roof are being renewed. This particular job is being handled by contractors' welders but on many occasions may be done by refinery welders. The liquefied petroleum gases must be stored under pressure and this is usually accomplished in spheres or spheroids. Figure 9 illustrates the field erection of a sphere.

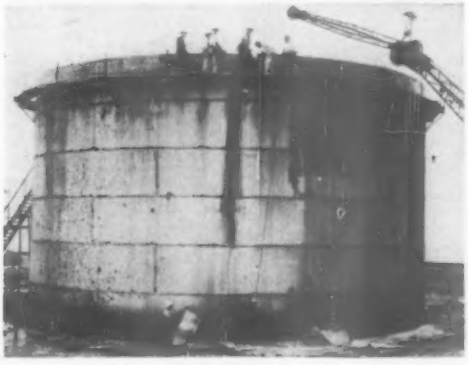

Fig. 8 Repairing tank

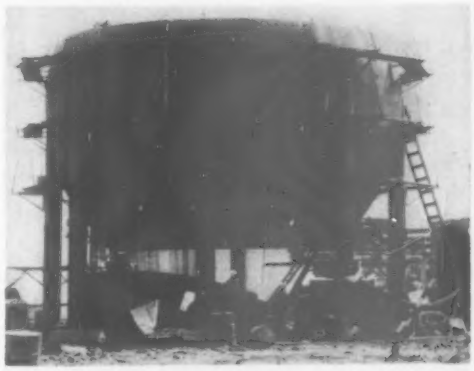

Fig. 9 Erecting sphere

ALLOY WELDING

Corrosion constantly presents a serious problem in refining operations and is combatted through chemical means intended to neutralize the corrosive agent or through the use of alloy materials which are resistant to the corrosive agent. In some cases, strength requirements such as for extremely cold or extremely hot services demand the use of alloy material. Corrosion and service conditions have led to the use of such alloy materials as killed-carbon steel; high-carbon steel; carbon-molybdenum steel; 5% chrome; 9% chrome; 13% chrome; 18–8 chrome nickel in types 304, 316 and 347; 25–12 chrome nickel; 25–20 chrome nickel; Durimet; Worthite; Hastelloy B and D; Monel;

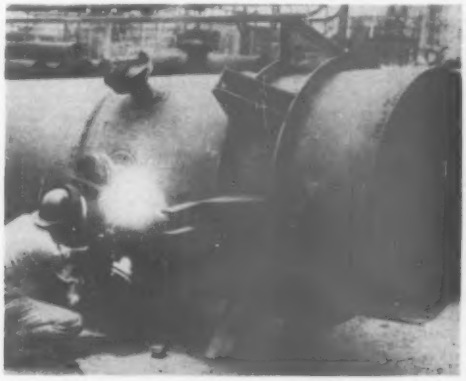

Fig. 10 48-In. alloy line

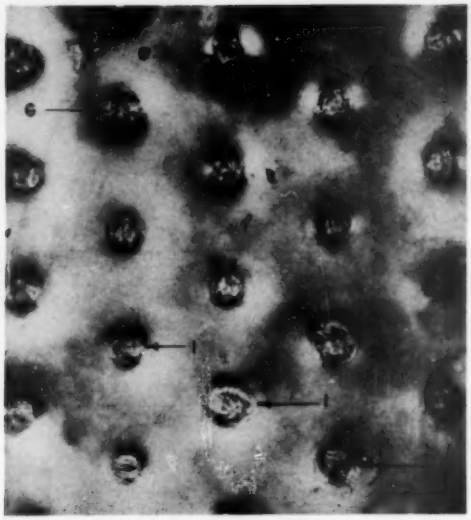

Fig. 11 Plug-welded liner

commercial nickel; low-nickel steels (approximately 1%); 3½% nickel steel; Cyclops; many of the brass alloys; copper; aluminum; Inconel; lead; and hard-surfacing materials, such as Colmonoy, Stoodite and Stellite. Clad materials where steel sheets are surfaced in Monel, nickel and 18–8 chrome nickel are also frequently used. Figure 10 shows a section of 48-in. piping fabricated from 18–8 chrome nickel which the welder is just finishing. This particular section is for one of the lines on a catalytic-cracking unit.

Figure 11 illustrates the plug-welded type of liner in which alloy sheets about $^{3}/_{32}$ in. thick are welded to the steel vessel wall through drilled or punched holes. Figure 12 illustrates a strip-welded alloy liner in which strips are installed in shingle fashion to the vessel wall. Figure 13 shows the field installation of a strip-welded Inconel liner in a crude distillation tower.

In small pieces of equipment where corrosion resistance is required, alloy liners which are built up by welding are frequently used. Figure 14 shows the installation of a welded Hastelloy-B liner in a centrifugal pump.

Fig. 12 Strip-welded liner

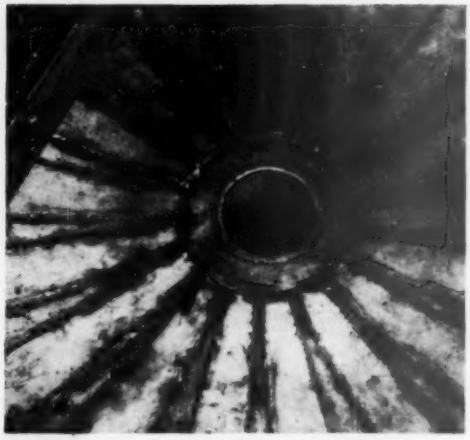

Fig. 13 Field installation of Inconel liner

A small amount of alloy welding in the form of hard surfacing is encountered in the refinery. This is principally confined to such things as pump sleeves and pump parts for reciprocating and centrifugal pumps. Hard-surfacing materials such as Colmonoy, Stoodite and Stellite have been used. Figure 15 shows the application of a hard-surfacing material on the screw of a screw-type positive displacement pump handling powdered catalyst.

WELDERS' QUALIFICATION TESTS

Welding quality control unquestionably begins with the employment of the welder. Every welder employed by the refinery and every welder employed by

Fig. 14 Hastelloy-B pump liner

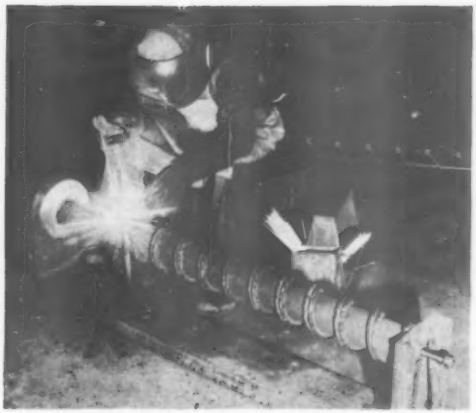

Fig. 15 Hard surfacing of catalyst screw

a contractor working in the refinery is required to demonstrate his ability to satisfactorily perform welds of the quality demanded through a qualification test.

It is the endeavor in refinery welding operations to create versatility and steer away from specialist in any particular type of welding. Welders must be equally good on pipe work, vessel fabrication and alloy welding. The only distinction that is made is between acetylene welders and metallic-arc welders. Qualification tests for both of these types are identical except that the welds are performed by the method for which the welder is endeavoring to qualify.

Qualification tests are twofold, consisting of a butt-welded plate test and a position pipe-welding test. Figure 16 shows the details of the specimens making up these two tests.

On the plate test, two pieces of A.S.T.M. A-285 firebox-grade carbon steel $^1/_2$ in. thick are butt welded together, all welding being performed from one side using A.W.S. E-6010 electrodes. Figure 17 shows a

Fig. 17 Overhead-plate test

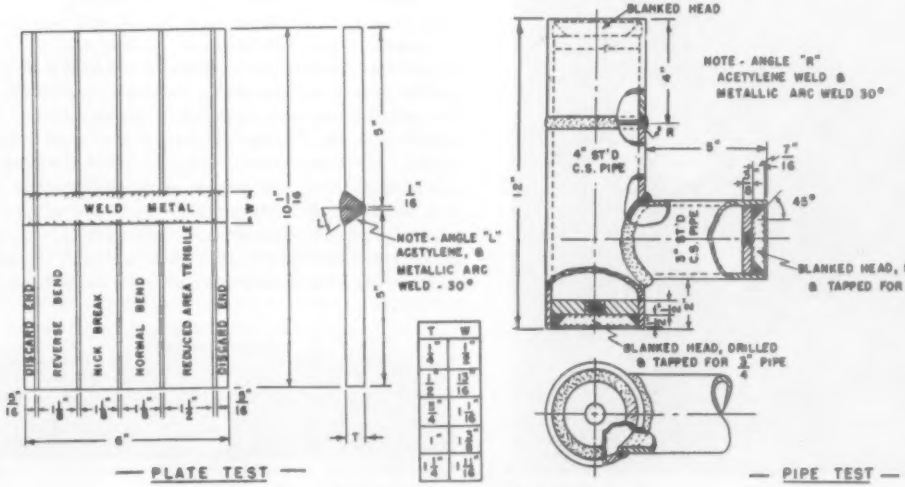

Fig. 16 Tests for welding operators

welder performing the plate test. In this test, the two pieces are tacked together and the specimen fixed in a position about head high. The elevation of the specimen may be altered at the discretion of the inspector, sometimes being as low as 24 in. above the floor. In all cases, however, the weld is an overhead weld.

The position pipe-welding test consists of welding up the closed specimen shown in Fig. 16. A.W.S. E-6010 electrodes are also used on this test. It is required that the specimen be mounted with the 4-in. run in a horizontal position with the 3-in. lateral pointed downward. The specimen is tacked together and then fixed in a location 24 in. from the floor and 8 in. from a wall and must remain so fixed until all of the welding has been performed. Figure 18 shows a welder actually performing a position pipe test.

During the qualification tests, the quality of the welding is carefully scrutinized by an inspector and the welder's ability to weld evaluated in general terms. The plate test is cut into four strips and physical tests conducted as shown in Fig. 16, thereby evaluating the physical properties of the weld. The pipe specimen is hydrostatically pressure tested at 1600 psi. and must show no leaks. Samples are cut from the welds and are etched and closely examined. In some cases X-ray examination may be made. A point system has been devised on such factors as general appearance, root penetration, slag inclusion, blowholes, reinforcement and physical properties. Using this system, each welder is graded. A welder must receive 80 out of a possible 100 points in order to satisfactorily qualify.

Welding operators employed by contractors are also required to pass qualification tests before they are permitted to weld on refinery equipment, but because of their special operations, contractor welders are permitted to qualify on either the plate test or position pipe test instead of being required to take both tests. In these cases, the welder may perform only the type of welds for which he took and passed qualification tests.

Where alloys are encountered, or where specified electrodes are different than those normally handled, or an unusual welding procedure is required, a welder may be required to qualify using the particular electrodes, materials and welding procedures specified for the work in question.

All welders, both refinery and contractor, who satisfactorily pass the qualification tests are assigned an identification stencil, and all welds made by a welder must be clearly marked with his stencil.

Fig. 18 Position pipe test

WELDING INSPECTION

The high quality of welding demanded in the refinery is assured through welding inspection. It has been found that one inspector can adequately cover the work performed by 20 to 30 welders when considering refinery welders. Somewhat closer inspection coverage has been found desirable for contractors' welders. The inspectors working among the welders observe the preparation of the joints for welding, the fit-up, the performance of welds and the completed welds. Where is is possible to observe a weld being performed through all of its stages up to completion, a decision is readily reached as to its suitability. Where this is not possible, representative welds are trepanned or plug samples are burned out. An endeavor is made to take at least 1 plug sample from each refinery welder every 3 months. In most cases, samples are cut at a much greater frequency than this, especially where welding is performed on pressure equipment. In general, the number of plug samples taken is based on the footage of welding performed, this often being prescribed by some construction code or job specification.

All plug samples are cleaned, etched and carefully evaluated. Figure 19 shows an etched sample cut from a representative pipe weld and exemplifies the high quality of welding required. Particular attention is devoted to laying in a good root or stringer bead. It will be noted in this figure that the root bead is extremely sound and projects inside of the pipe wall approximately $^1/_8$ in. In order to obtain this quality in pipe welding, continued practice led to the development of a dragging technique for pipe welding. A $^1/_8$-in. A.W.S. E-6010 electrode is used with the welding machine set at normal amperage and voltage for the

Fig. 19 Pipe-weld sample

electrodes selected. The arc is struck and immediately the electrode is pressed deeply and firmly into the root of the joint and is dragged around the pipe. In placing this bead almost all of the arc will be inside of the pipe. In straight down-hand work, the electrode is dragged slowly enough to allow depositing the weld metal as it progresses. In overhead and vertical welds, essentially the same technique is used, except a series of dragging movements approximately 1 in. long working uphill are resorted to. After depositing the root or stringer bead, $^{5}/_{32}$- or $^{3}/_{16}$-in. electrodes are used and the weld completed. The reinforcement should project about $^{1}/_{16}$ in. above the pipe wall and should be well rounded from the edges to the middle.

Where the sample reveals bad welding, it is brought to the attention of the welding foreman and the joint is cut out and rewelded. This matter is brought to the attention of the welder who performed the weld, and in an instructive rather than critical manner, the defects are pointed out to him and he is advised how these may be overcome. When a welder performs a substandard weld and the matter has been brought to his attention, the work performed by this welder is closely followed until an improvement has been made in the quality of his work. This has resulted in a keen appreciation by the welders of the high quality demanded and it has become extremely rare that substandard welding samples are taken. It has been brought out that each welder has an identification stencil assigned to him and each weld performed by him must be prominently marked with his stencil. A welding record is maintained on each welder, both refinery and contractor, and all information pertaining to the qualification test and quality of welds obtained in welding samples is recorded thereon.

WELDING PROCEDURES

A good measure of welding quality control is obtained through the development and execution of proper welding procedures. The inspection group has experienced welders and metallurgists working in close cooperation, and often on abnormal jobs, special welding procedures are developed and job specifications outlining these procedures are issued. A carefully planned operation naturally produces more successful and better quality welding than hit or miss welding operations conducted during the performance of the actual job. In some cases some experimentation may be conducted to determine a suitable welding procedure, while in other cases it may be a job where experienced welders and trained metallurgists are able through experience and knowledge to work out a procedure in advance which will facilitate the welding operation.

Heavy weldments such as illustrated in Fig. 5 are an example of this type of welding quality control. In this installation, the vessel wall is 2 in. thick, the reinforcing ring $1^{1}/_{2}$ in. thick. Prior to welding, the area surrounding the manhole was preheated with acetylene torches from 300 to 400° F. Two welders were used on the inside and two welders on the outside. The four welders began welding simultaneously, the outside welders beginning diametrically opposite on the horizontal axis and the inside welders diametrically opposite on the vertical axis. By this means, all of the welders were welding at a different point. The weld was deposited in a cascade manner, that is, a short bead is run out, then the welder steps back and puts the second pass on the weld already deposited. He continues to step back on the deposited weld until the joint is completely filled. Using successive operations of this type, he progresses around the welded joint. The thickness involved was such that the A.P.I.-A.S.M.E. Code required stress relieving. After all welding was completed, a gas-burner ring was used to raise the metal temperature in the area of the attachment, and thermal stress relief was carried out in accordance with the code requirements. The burner ring permitted heating to about 1050° F. Jobs of this type are usually covered by welding procedures or job specifications.

The installation of 18–8 chrome nickel and Monel vessel liners is frequently done in the refinery. Although this has become a common practice, it has been found that the jobs are facilitated by preparing and issuing welding procedures or job specifications on each of the liner installations. These specifications establish whether the liner is to be plug welded or strip welded and specify the spacing and size of plug holes or the width of the lining strips. The type and size electrodes desired are also specified.

The use of air-hardening steels such as the straight chrome alloys, 5, 9 and 13% chrome; low-nickel steels in cold services where impact at subzero temperatures is important; Monel, where iron pick-up may seriously reduce corrosion resistance; and austenitic steels, such as 18–8 chrome nickel Type 304, where intergranular corrosion may be a factor, are other examples where it has been necessary to develop welding practices and prepare job specifications.

EVALUATION OF WELDING ELECTRODES

Welding-quality control is also exercised through the evaluation of welding electrodes. Periodically, electrodes in the A.W.S. classifications normally used in the refinery are obtained from a large number of the manufacturers of welding electrodes. These electrodes are tested comparatively from the standpoint of general weldability, acceptability by the welders, chemical and physical properties of the deposited weld metal, amount of slag deposition and ease of its removal and durability of the coatings both during welding and in storage. From these tests, a group of suppliers is determined who can furnish electrodes in each classification that satisfactorily meet the requirements, and purchases of the electrodes are limited to the suppliers of the approved rods. Between the periods of these large-scale tests, individual tests are frequently conducted to evaluate electrodes not previously tested.

Fig. 20 Specimen for electrode evaluation

Figure 20 shows the filling of a 2-in. angle with deposited weld metal. From this deposited weld metal a 0.505-in. diameter tensile specimen will be machined and physical properties obtained. A chemical analysis of the deposited metal will also be made. During the preparation of this sample and other testpieces, the other criteria listed above are evaluated.

WELDING RESEARCH

Under welding quality control, welding research undoubtedly contributes a good measure. It is the responsibility of the inspection group through the cooperation of the experienced welders and metallurgists in this group to keep abreast of and investigate developments in the field of welding.

In this work perhaps notable achievement has been made in the refinery in the field of cast-iron welding. Various cast-iron welding techniques have been experimented with and several methods have been developed by which cast iron can be successfully welded by the metallic-arc process. Figure 21 illustrates the repair of a cast-iron compressor cylinder. A preheat furnace can be seen and the welders are noted on top of the furnace actually performing the welding. In this job, the cylinder was uniformly preheated to about 600° F. and a cast-iron a.-c. electrode was used. Successful techniques have also been developed for the use of the high-nickel electrodes commonly used for electric-arc welding of cast-iron materials. The welding of cast iron is very complex and often the welding procedures must be altered to meet the conditions. Therefore, with this type of work, several methods have been developed, some requiring preheat and others requiring no preheat, which have proved successful. Through careful study of the particular job, it is possible to select a suitable welding procedure and perform electric welding on cast iron successfully.

In the handling of chemicals in the refinery, a large amount of copper or copper alloy is used. Corrosion in this equipment has necessitated welding repairs and the carbon-arc procedure for welding these materials has been used successfully.

Fig. 21 Welding cast-iron cylinder

Developments such as submerged-arc welding, shielded inert-gas-arc welding, powder cutting, oxy-arc cutting, stud welding and a.-c. welding are investigated and their adaptability to refinery uses evaluate Perhaps the most notable achievement in this field was the decision to use a.-c. welding in the performance of certain types of welds principally involving shop work. Until this time, only d.-c. welding machines had been used. While both a.-c. and d.-c. welding enjoy certain advantages and disadvantages, it has been found desirable to use both types of welding.

CONCLUSIONS

In conclusion, it should be brought out that welding is so closely related to welding quality control that it is difficult to segregate the two into distinct items. The subject has been presented principally from the standpoint of welding quality control, but the things which have been achieved were brought about through the close cooperation of the Welding Dept. which supervises the welding operations and the Equipment Inspection Dept. which supervises the welding quality control. Through the efforts of these two groups, an extremely high quality of welding is realized and welding is accomplished in a most efficient manner.

Reiterating, the principal points of welding quality control are:

1. Selection of proper personnel through welders qualification tests.

2. Follow-up in welding operations through welding inspection, consisting of observation of jobs in prog-

ress, sampling and evaluation of finished welds, constructive instructions to welders who perform substandard welding and maintenance of welding records.

3. The development and use of suitable welding procedures, and facilitating welding operations through the preparation of job specifications.

4. The testing and evaluation of welding electrodes to compare their relative merits and to classify suppliers of acceptable electrodes.

5. Conducting welding research so that welding procedures may be tested and evaluated, and investigating the latest developments in the field of welding so that their adaptability to refinery uses may be determined.

Professional Training for the Welding Engineer

Welding courses at the Rensselaer Polytechnic Institute for undergraduate and graduate students

by Robert A. Wyant

A WELDING laboratory was established in the Department of Metallurgical Engineering at the Rensselaer Polytechnic Institute in 1937. Shortly thereafter an extensive research program was undertaken and publication of papers describing the work was commenced. As a result of this research activity the laboratory gained considerable prestige in the welding field. Students with strong interests in welding began to be attracted to the school and industry began to inquire about the availability of men who had received some training in welding at the engineering level. On the basis of this experience the Rensselaer Polytechnic Institute decided that there was a need of professional training for the welding engineer and proceeded to provide appropriate courses and laboratory facilities for its engineering students at both the undergraduate and graduate levels.

UNDERGRADUATE WELDING OPTION PROGRAM

Undergraduate students who expect to follow careers in the welding engineering field normally enroll in the Department of Metallurgical Engineering. Such students pursue the same studies as the regular metallurgical engineering students for the first three years and a special sequence of courses known as the "welding option" for the fourth and last year. The complete curriculum follows:

METALLURGICAL ENGINEERING

FIRST YEAR

First Term	*Second Term*
Chemistry I	Chemistry II
Engineering Drawing	Historical Brackground of the Modern World
English I (Introduction to World Literature)	Descriptive Geometry
Introduction to Engineering and Science	Calculus II
Calculus I	Physical Training II
Physical Training I and Hygiene	Physics II (Mechanics and Heat)
Physics I (Mechanics)	

SECOND YEAR

First Term	*Second Term*
Analytical Chemistry	Metal Casting, Forming and Welding
English II (Masterpieces of Literature)	Principles of Metallurgy
Calculus IIIa	Physical Geology
Shop Processes	Physical Training IV
Physical Training III	Physics IV (Acoustics, Optics, and Electronics)
Physics III (Electricity and Magnetism)	Engineering Mechanics

THIRD YEAR

First Term	*Second Term*
Physical Chemistry I	Physical Chemistry II
Economic and Social Principles	Economic and Social Problems
Electrical Engineering Theory	Electrical Engineering Laboratory
Production Metallurgy	Metallography
Strength of Materials	Welding Processes and Applications
	Physical Metallurgy I

FOURTH YEAR (METALLURGY OPTION)

First Term	*Second Term*
Physical Metallurgy II	Applied Composition
Nontechnical Elective	Electrometallurgy
Technical Elective	Heat Engines
Metallurgical Thermodynamics	Nontechnical Elective
Chemical Engineering Theory	Technical Elective
	Thesis or Technical Elective

FOURTH YEAR (WELDING OPTION)

First Term	*Second Term*
Elementary Structural Analysis	Elementary Structural Design
Heat Engines	Nontechnical Elective
Physical Metallurgy II	Technical Elective
Welding Engineering	Thesis or Technical Elective
Nontechnical Elective	Applied Composition
	Machine Design

This curriculum includes not only the basic courses which are considered essential for undergraduate engineering students but also courses in the specific areas of welding, metallurgy and design. It is these very areas which are emphasized in the tentative A.W.S. definitions of "welding engineer" and "welding engineering." The curriculum provides a sequence of three courses dealing specifically with welding:

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Paper presented at "Symposium on the Need of Professional Training for the Welding Engineer" held during Annual Meeting, A.W.S., Chicago, Ill., week of Oct. 22, 1950.

Year	Course
2nd	Metal Casting, Forming and Welding
3rd	Welding Processes and Applications
4th	Welding Engineering

The student is introduced to welding in "Metal Casting, Forming and Welding" where the treatment of the subject is of necessity elementary and brief. At present the text material consists of notes which have been prepared and mimeographed especially to meet the needs of the course. In "Welding Processes and Applications" the student gets a thorough treatment of the theory of all welding and allied processes, and laboratory experience with many of the processes. In addition the student gets an elementary treatment of welding metallurgy, distortion, residual stress and design as related to welding. At present the text material consists of "Welding Technology" by F. Koenigsberger (Cleaver-Hume Press, London, 1949) supplemented by notes which have been prepared and mimeographed especially for the course. In "Welding Engineering" the student gets a more advanced treatment of welding metallurgy, distortion, residual stress and design. In addition considerable attention is given to such subjects as the thermal aspects of welding, production engineering problems in welding and the functions of welding engineers in industry. One of the objectives of the course is to acquaint the student with the literature of his chosen field. Each student is required to plan and execute a well-balanced reading program making extensive use of all important welding literature. In the "welding option" the student is required to take a sequence of two courses in structural analysis and design, and one course in machine design during the fourth year. Students who take this option are expected to do an experimental thesis on some welding problem during the last term of the course. Upon completion of this course of studies the student receives the degree of "Bachelor of Science in Metallurgical Engineering." It must be emphasized that students who take the "welding option" in the metallurgical engineering course get essentially the same basic training as the regular metallurgical engineering students. As a consequence, these students are well qualified for starting positions in either the metallurgical or welding engineering fields, or where a combined knowledge of metallurgical engineering and welding is needed. It should also be mentioned that students in mechanical engineering who take "Welding Processes and Applications" and "Welding Engineering" in sequence as elective courses are also well qualified for starting positions in the welding engineering field.

GRADUATE PROGRAM

At the graduate level the Rensselaer Polytechnic Institute conducts a very successful program for training engineers for the welding field. This program has now been in operation for over ten years. Graduate students in the Department of Metallurgical Engineering who are interested in welding are assigned to part-time work on welding research projects. This plan provides the student with an excellent opportunity to become familiar with the experimental equipment and techniques involved in the investigation as well as the theoretical reasoning behind the work. The research projects are a rich source of problems for graduate theses at both the master and doctorate levels. Students frequently receive some remuneration for their services on research projects which helps to defray the expense of their graduate training. The following courses dealing with welding are taught for the benefit of graduate students: Welding Processes and Applications, Electric Resistance Welding, Electronic Control and Power Supply for Resistance Welding, Instrumentation of Welding Processes and a Graduate Welding Seminar.

GENERAL UNDERGRADUATE PROGRAM

The Rensselaer Polytechnic Institute also provides training in welding for students other than those who have definite interests and ambitions in that field. For example, the course, "Metal Casting, Forming and Welding," is required of all students in the fields of aeronautical, mechanical and metallurgical engineering. A somewhat similar course, "Metal Processing," which includes an even briefer treatment of welding is required of all students in chemical engineering. The course, "Welding Processes and Applications," is required of all students in metallurgical engineering. A course, "Metallurgy and Welding," is required of all students in civil engineering who take the foundations, structures or transportation options. All of the above courses and also the course, "Welding Engineering," are available as "electives" to all qualified students regardless of their field. For example, the course, "Welding Processes and Applications," is frequently taken by students in mechanical, electrical and management engineering. One or more of the above courses are frequently recommended for students in management engineering who take either the mechanical or metallurgical options.

RESULTS

The demand for young engineers trained in welding exceeds the supply in the experience of the Rensselaer Polytechnic Institute. This is particularly true at the graduate level. Since the welding laboratory was established in 1937 about 31 graduate students have participated in the graduate training program. Of these men, nearly one-half are now in important industrial and academic positions where welding is of primary importance. The first students to take the undergraduate welding option were graduated in 1948. Only eleven men have been graduated with this option to date. At the present time the "welding option" program has neither been in operation long enough nor been given adequate publicity to justify any conclusions as to the success of the program.

Need for Professional Training for the Welding Engineer

The need for professional training of welding engineers is greater today because of the accelerated technical developments in the use of new metals and alloys in the fabricating industry

by John J. Chyle

THE need for professional training for the welding engineer in the fabrication industry is greater today than it has ever been in the past.

This need for training has been created by the invention of new welding processes, the rapid development and use of new and old welding processes and the availability of new metals and alloys in the fabrication industry.

This need for professional training is also the result of keen competition in the fabrication industry, which makes the most efficient use of labor and materials mandatory.

Twenty-five years ago when I entered into the welding fabrication industry, only a few welding processes were in use, and the welding industry was in its infancy. Today there are over thirty separate and distinct welding processes which are used in the fabrication industry.

Within the last ten years many new metals and alloys have been made available to industry which are now being fabricated by welding. Some of the new metals for example, are zirconium, titanium, tungsten, molybdenum and vanadium.

Some of these new metals are fabricated for special military purposes only, and in a limited way, but indications are that they will find many industrial applications in the near future. Many new complex alloys for high-temperature service have been developed, which are used for both military and nonmilitary purposes.

Developments in welding processes and procedures, especially as the result of World War II, have tremendously accelerated welded fabrication of high-strength alloy steels. In the nonferrous field too, a tremendous increase in the fabrication by welding of aluminum, magnesium, copper, nickel and their alloys has taken place.

In the welding of carbon steel, advances have been made chiefly in the application of automatic welding and in the development of improved and new types of metal-arc welding electrodes.

In the welding fabrication industry today, keen competition has resulted in the development of labor-saving automatic welding equipment. The ever increasing cost of labor has stimulated welding engineers to conserve labor by the use of labor-saving devices such as special welding fixtures and the use of automatic welding processes. Competition has also resulted in reducing cost by reducing excessive or useless weight of metals being fabricated. This conservation of metal has been brought about largely by the welding engineers in redesigning the parts being fabricated for more economical welding. In many cases the design has not only resulted in less weight and welding cost, but also in improvement in strength or other mechanical properties.

The modernization in welding which has occurred in the fabrication industry has made new demands on welding engineers, who must meet these changing conditions by being adequately trained technologically as well as professionally.

What professional training should a welding engineer of today have?

Before answering this question, may I read to you the definition of the Practice of Welding Engineering, as submitted by the Educational Committee of the AMERICAN WELDING SOCIETY? This definition, in my opinion, expresses very clearly the duties and functions of a welding engineer, and concurs with my twenty-five years experience in the fabrication industry. Let me quote part of this definition.

"The practice of welding engineering involves a knowledge of three basic areas in engineering: one, the design of machines, structures and equipment as well as the design of suitable connections; two, the materials of engineering with particular reference to metals, gases and refractories; and three, the processes, procedures and equipment of the welding industry..."

The first two essentials—a knowledge of the design of machinery, structures and equipment—and a knowledge of the materials of engineering, namely

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Presented at Thirty-first Annual Meeting A.W.S., Chicago, Illinois, week of Oct. 22, 1950.

metals, gases and refractories require that the welding engineer receive basic training in these subjects. In fact, these subjects have always been given to engineering students in the past.

Today these subjects must be brought up to date—they must be streamlined or reduced to bare essentials, and they must be given as basic studies. A prerequisite of these studies, of course, is a fundamental knowledge of the sciences, namely mathematics, physics and chemistry. A thorough understanding of these is the basis of all engineering. Mathematics is not only a tool in the solving of engineering problems, but its value is even greater in that it helps the student to think clearly and logically, which is of utmost importance to a welding engineer. A clear understanding of chemistry and physics is indispensable because of the rapid advances in metallurgy, which is becoming increasingly more important in our everyday life. Besides a sound or an adequate knowledge of these sciences, it is important that the welding engineer have a thorough training in mechanics, both theoretical and applied, and a well outlined or planned course in stress analysis.

The professional training of a welding engineer must include a knowledge of the properties of materials, concentrating on metals, with careful training in the metallurgy of the metals used in industry. The training must also include extensive studies in electricity and a thorough understanding of electronics.

So far we have discussed only the broad engineering training which any engineer should have and which is also a requisite for the welding engineer. The professional training must also include well organized welding engineering courses. Referring to the definition of the Practice of Welding Engineers, Part III deals with a knowledge of the processes, procedures and equipment of the welding industry. This latter training is highly important and in this respect the colleges and universities should work closely with industry and the American Welding Society in the presentation of these logically arranged welding engineering courses. This must include a thorough understanding of the various processes available.

The training should include some practical aspect in these studies by having laboratory courses for practical demonstrations and participation by the students. Summer work in fabrication plants is highly recommended.

The welding engineering courses should include welding design, to be given by a competent and practical instructor. The colleges and universities should seek instructors who have had practical experience or who have contact with industrial organizations.

A separate course should be given on the fundamentals of the various inspection processes which are available and which are used in the welding fabrication industry in the inspection and evaluation of welds. This course would require a thorough understanding of radiographic examination, magnetic inspection and dye penetrant methods.

A course should be given to acquaint the welding engineering student with the various codes which govern the construction of pressure vessels and weldments, together with the various government specifications and regulations on weldments.

The student should also be trained in the classification of metal-arc welding electrodes. He must become familiar with the American Welding Society classification of electrodes, welding symbols and must know how to qualify welding operators and welding procedures.

The emphasis in the professional training of welding engineers must always be on broad training of fundamentals and principles rather than details. The engineer must have sufficient time to take other courses of a nontechnical nature. He must have some training in economics, psychology, philosophy, history and in the humanities. The problem of our universities and colleges is not to add to the length of the present engineering courses, to give this broad engineering training, but to streamline the courses so that only the fundamental subjects be given.

The emphasis today should be made to train the welding engineer along professional lines rather than along strictly technological lines. While there is need in industry for many welding technologists and welding specialists, the welding engineer is a combination of these qualifications and his approach to welding problems is from a broad viewpoint.

The schools and universities must also train the welding engineer how and where to find technical data.

One other quality is of importance to a welding engineer and that is the ability to get along with other people. He must direct the efforts of others when he executes his duties in supervision and in construction of welded fabrication. In consultation and planning he must use tact and diplomacy in order to convey his ideas to others. He must think clearly and logically in investigations and solutions of difficult welding problems.

To repeat, the need for professional training of welding engineers is greater today because of the accelerated technical developments in the use of new metals and alloys in the fabrication industry. The welding fabrication industry is looking for professionally-trained engineers to lower welding costs, by reducing labor costs and material costs, in order to be competitive. The welding engineers' training must be broad in nature, stressing the importance of a sound general engineering training, a basic welding engineering training and a clear understanding of the arts and sciences, in order that he may have the professional rather than the technological attitude toward his work.

Nickel Portion of A.W.S. 1950 Educational Lecture Series

Delivered as part of October 1950 Educational Lecture Series at Annual Meeting of American Welding Society—properties high-nickel alloys, procedures using metal arc, inert-gas metal arc, atomic hydrogen, gas, resistance welding, brazing and cutting

by R. M. Wilson, Jr.

1951 is the 200th anniversary of the discovery of the element nickel by the Swedish scientist Cronstedt. Prior to that time, nickel had been an unconscious alloying element in several ancient metal-working instances. Paktong, a Chinese metal made by adding zinc to what we now know to have been nickel-copper ores, was brought to Europe by The East India Co. in the 17th century. It is believed that ancient oriental peoples learned at an early date to make useful implements out of meteorites which frequently contain nickel and iron. Thus the tradition that the swords of the great warriors of China, Persia and northern Europe were "Heaven-sent" may have more than fancy for a background.

The first large use of nickel, other than coinage, nickel silver (German silver) or plating, occurred during the first World War when nickel-bearing armor plate was sought after by the navies of the world. With the cessation of hostilities and the Geneva Disarmament Conference, 90% of the market for nickel disappeared.

Principally because of an extensive research and development program undertaken by The International Nickel Co. there are today over 3000 alloys in which nickel is a part, from 99.7% in malleable nickel to 0.2% in a hardenable silver alloy. Table 1 illustrates the consumption of nickel in the United States for 1948 and 1949. It will be noted that the principal use of nickel is in corrosion-resisting applications. Stainless steels, some cast irons, nonferrous alloys, high-temperature electrical-resistant alloys and plating may be classed as applications where corrosion resistance is of prime importance and these applications account for approximately $^3/_4$ of the total consumption.

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Presented at the Educational Lecture Series during the Annual Meeting, A.W.S., Chicago, Ill., week of Oct. 22, 1950.

Table 1—Consumption of Nickel in the United States (Data from the U. S. Bureau of Mines)

Uses	*1948, %*	*1949, %*
Ferrous	45.2	42.2
Stainless steels	17.4	17.5
Other steels	23.3	19.7
Cast iron	4.5	5.0
Nonferrous	30.0	27.7
Comprises copper-nickel alloys, nickel-silver, brass, bronze, beryllium, magnesium and aluminum alloys and Monel, nickel and Inconel.		
High-temperature electrical-resistance alloys	6.6	6.0
Electroplating	15.8	21.3
Other	2.4	2.8
	187 Million lb.	137 Million lb.

This discussion will be limited to the high-nickel alloys; those containing over 50% nickel. In the interests of brevity, only those alloys produced at the Huntington, W. Va., and the Bayonne, N. J., Works of The International Nickel Co., Inc., will be discussed in detail. The procedures to be discussed will apply with equal force to other high-nickel alloys.

The high-nickel alloys may be generally classified in three groups as shown in Table 2. The nickel group contains nickel with an average carbon content of 0.06% which is used in all applications subzero to 600° F. In the range of 600° to 1200° F., carbon precipitates as graphite with a resulting loss in ductility. Low-carbon* nickel contains 0.02% carbon or less and may be used at elevated temperatures without fear of graphitization. Duranickel* is an age-hardenable alloy containing aluminum which will respond to thermal treatment. Nickel-clad steel is produced by Lukens Steel Co.

Monel* is an alloy containing approximately $^2/_3$ nickel and $^1/_3$ copper. It is one of very few alloys which are produced

* Reg. U. S. Pat. Off.

Table 2—General Classification of High-Nickel Alloys

Form	*Nickel*	*Nickel-Copper*	*Nickel-Chromium-Iron*
Wrought	Nickel	Monel	Inconel
.....	Low-carbon nickel	"K" Monel	Inconel "X"
.....	Duranickel	"R" Monel	Inconel-clad
.....	Nickel-clad	Monel-clad	.....
Cast	Nickel	Monel	Inconel
.....	.....	"S" Monel	.....
.....	.....	"H" Monel	.....

directly from ore containing the major alloying elements. "K"* Monel is an age-hardenable alloy containing aluminum which will respond to thermal treatment. "R"* Monel is an alloy containing sufficient sulphur to improve machinability. Monel-clad steel is produced by Lukens Steel Co.

Inconel* is an alloy containing 77.0% nickel, 15.0% chromium and the balance iron. Inconel "X"* is an age-hardenable variation of Inconel which contains titanium, aluminum and silicon. Inconel-clad steels are produced by Lukens Steel Co.

Castings of Monel, nickel and Inconel of nominal compositions are produced in two grades. One grade may not be of weldable quality. The other grade is of weldable quality.

"S"* Monel and "H"* Monel are cast Monels containing silicon. They are not to be used where welding is employed. "S" Monel and "H" Monel will respond to age-hardening thermal treatments.

These alloys represent the bulk of the production of high-nickel alloys. There are approximately 50 important high-nickel alloys which are produced in lesser volume but which time does not permit mentioning.

Table 3 shows the nominal composition of these alloys.

* Reg. U. S. Pat. Off.

Table 4 shows the average mechanical properties of these materials as would be obtained from 1-in. diameter bar. Note that these range from a low of 50,000 psi. tensile, 10,000 psi. yield strength for annealed low-carbon nickel to a high of 200,000 psi. tensile and 140,000 psi. yield strength for Inconel "X" in the age-hardened condition. It is evident that these materials have mechanical properties in the range of the low-alloy and high-alloy steels.

In elevated temperature applications they maintain well these high-mechanical properties. In subzero temperature applications, the tensile properties increase as the temperature is lowered while elongation, reduction of area and impact remain substantially constant.

Table 5 shows a few of the physical properties of the high-nickel alloys. They have high melting points which permit the use of any welding or brazing process which can be used on steel. The thermal expansion is not too different from that of steel, therefore it is safe to couple these alloys with steel. Their heat-transfer coefficients are not as high as those of copper or aluminum, but are usefully high particularly when it is remembered that corrosion films are generally absent. Corrosion films can lower the over-all coefficient of heat transfer very drastically. The electrical resistance of the high-nickel alloys range from about the same as steel in the case of nickel and very much higher in the case of Inconel "X." Nickel and low-carbon nickel are relatively magnetic though not as ferro-magnetic as steel. Monel and "R" Monel are slightly magnetic at room temperature and nonmagnetic above 140° F. The other alloys are nonmagnetic down to very low temperatures.

The work-hardening characteristics of the high-nickel alloys are shown in Fig. 1. It will be noted that nickel and low-carbon nickel respond similarly to steel while the alloys have greater response but somewhat less than stainless steels.

All high-nickel alloys are subject to attack by sulphur, lead and other low melting-point alloys at elevated temperatures. Nickel is the most susceptible, while Inconel is most resistant. The attack is intergranular producing a brittle phase which cracks when stressed. Once such embrittlement has occurred there is no method to salvage the material. It must be scrapped. It is highly important that strict cleanliness be observed when fabricating the high-nickel alloys. All oil, grease, drawing lubricants, paint, marking

Table 5—Average Physical Constants of High-Nickel Alloys

	Melting point, ° F.	*Coefficient of thermal expansion 10^{-6} x in./in./° F., 32–212° F.*	*Coefficient of thermal conductivity Btu./ft.²/hr./° F./in., 32–212° F.*	*Electrical resistivity ohms/cir. mil. ft., 32° F.*	*Permeability H = 200 Oersteds 80° F.*	*Curie temperature, ° F.*	*Density lb./cu. in.*
Nickel	2615–2635	7.2	420	57	30.0	680	0.321
Low-carbon nickel	2615–2635	7.2	420	50	30.0	680	0.321
Duranickel	2615–2635	7.2	135	260	10.0	200	0.298
Monel	2370–2460	7.8	180	290	6.0	110–140	0.319
"K" Monel	2400–2460	7.8	130	350	1.0015	−150	0.306
"R" Monel	2370–2460	7.8	180	290	6.0	110–140	0.319
Inconel	2540–2600	6.4	104	590	1.006	−40	0.307
Inconel "X"	2540–2600	7.2	87	750	1.0028	−280	0.300
"H" Monel	2350–2400	6.8	180	370	1.006	−70	0.305
"S" Monel	2300–2350	6.8	180	380	1.006	−70	0.302

Table 3—Nominal Composition of High-Nickel Alloys, %

	Ni	*Cu*	*Fe*	*Mn*	*Si*	*C*	*S*	*Al*	*Cr*	*Ti*	*Cb*
Nickel	99.4	0.1	0.15	0.25	0.05	0.05	0.005	..	..	..	..
Low-carbon nickel	99.4	0.05	0.10	0.20	0.15	0.01	0.005	..	..	..	..
Duranickel	93.7	0.05	0.35	0.30	0.50	0.17	0.005	4.4	..	..	..
Monel	67.0	30.0	1.40	1.00	0.10	0.15	0.01	..	..	..	..
"K" Monel	66.0	29.0	0.90	0.75	0.50	0.15	0.005	2.75	..	..	..
"R" Monel	67.0	30.0	1.40	1.00	0.05	0.15	0.035	..	..	..	..
"S" Monel	63.0	30.0	2.00	0.75	4.00	0.10	0.015	..	..	..	..
"H" Monel	63.0	31.0	2.00	0.75	3.00	0.10	0.015	..	..	..	..
Inconel	77.0	0.20	7.00	0.25	0.25	0.06	0.007	..	15.0	..	..
Inconel "X"	73.0	0.05	7.00	0.50	0.40	0.04	0.007	0.90	15.0	2.50	1.00

Table 4—Nominal Range of Mechanical Properties of High-Nickel Alloys 1-In. Diameter Rods

Wrought-annealed-cold drawn	*Tensile strength, 1000 psi.*	*Yield strength 0.2% offset, 1000 psi.*	*Elongation % (2 in.)*	*Hardness Brinell*
Nickel	55–110	15–100	55–10	90–230
Low carbon nickel	50–100	10–90	60–10	75–200
Duranickel*	150–210	110–175	30–15	285–380
Monel	70–125	25–120	50–17	110–250
"K" Monel*	130–170	90–130	30–15	240–325
"R" Monel	85–115	50–100	35–15	160–240
Inconel	80–150	25–125	55–10	120–290
Inconel "X"*	110–200	45–140	50–20	220–400
Cast-range				
Nickel	60–70	20–35	35–15	80–125
Monel	65–90	32–45	50–25	125–150
"H" Monel*	85–120	50–80	20–10	175–250
"S" Monel*	120–145	80–115	4–1	300–375
Inconel	70–95	30–45	30–10	160–190

* Age-hardened condition.

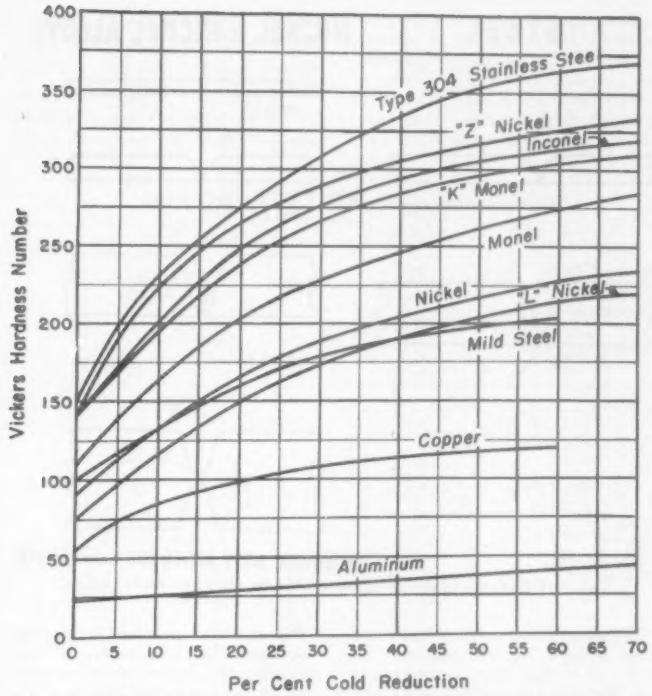

Fig. 1 Work-hardening characteristics of high-nickel alloys and other metals

pencil marks and other materials which might contain either sulphur or lead must be removed prior to any welding or heating operation. Sulphur can attack at temperatures as low as 700° F. Lead attack has been recorded at 400° F. The higher the temperature and the longer the time, the more serious will be the attack. Figure 2 shows the characteristic dried mud appearance of sulphur embrittled nickel-clad steel which was heated over a coke forge for forming. Coke or coal always contains too much sulphur for such use. Fuel oil is satisfactory if the sulphur content is less than 0.5% (No. 1 grade). City gas is useful if the sulphur content is less than 10 grains per 100 cu. ft. Figure 3 shows sulphur and lead embrittlement of Monel during arc welding. A previously lead-lined steel tank for the splitting of fatty acid was being relined with Monel. Adequate cleaning was not done. Thorough cleaning prior to repair welding is particularly important.

None of the high-nickel alloys will harden when quenched from a high temperature. Furthermore, the rate of cooling, except for the age-hardenable alloys Duranickel, "K" Monel, and Inconel "X" may be fast or slow with no effect upon the end result. Age-hardenable alloys should be cooled rapidly. This means that preheating is not necessary prior to welding, and in general no postweld heating is necessary to improve the metallurgical characteristics of the weld and adjacent area. Naturally there will be stress produced in a weld as molten metal soldifies and cools to room temperature. Normally stress relieving treatments to remove these stresses are not necessary. The A.S.M.E. Boiler Construction Code, Unfired Pressure Vessel Section, does not require stress relieving for safety requirements in walls under $^3/_4$ in. thickness. Special stress relieving procedures may be required where unusual corrosive environments are encountered, or where exceptional dimensional stability is necessary. Stress relieving of the age-hardenable alloys after welding and before age hardening may be required in very stiff structures; since age-hardenable alloys have very low ductility at age-hardening temperatures.

As a general rule, major welding operations should not be attempted on the harder tempers of material. Welding on material over 200 Brinell may result in cracking if the structure is exceptionally stiff. Like all general rules, this one is subject to exceptions. Welding can sometimes be done successfully on cold-worked material but it is not recommended. Welding on age-hardenable alloys should be done when the material is in the annealed or stress-relieved conditions, prior to aging.

Any welding process commonly used for fabricating steel may be employed in fabricating the high-nickel alloys. Naturally there are some modifications of procedure necessary due to the different characteristics of the high-nickel alloys, but any vessel or structure fabricated from

Fig. 2 Sulphur embrittlement of nickel-clad steel; heated in coke-fired forge prior to bending

Fig. 3 Effect of sulphur and lead in welding of a Monel lining to an existing steel vessel

steel can also be fabricated from high-nickel alloys.

METAL-ARC WELDING

Manual metal-arc welding with flux-coated electrodes is most universally used for welding the high-nickel alloys in thicknesses 0.050 in. and up.

The design for welding is similar to that employed for steel except that somewhat more room for manipulation is required in a weld groove. The high-nickel alloys are more sluggish than steel under an arc and require the use of a greater included angle. Figure 4 shows the basic design for butt joints in the high-nickel alloys as well as the designs employed for steel. Note that a 75° included angle is recommended for high-nickel alloys as a contrast to 60° included angle for steel. Since high-nickel alloys are mainly used in corrosive environments, it is necessary to consider the surface in contact with the corrosive. This surface should be as smooth as practical, in order to eliminate pits and crevices where corrosion can start. Complete penetration without the use of backing strips is a general requirement. On material 0.109 in. and over it is good practice to use a seal bead on the back rather than to try and obtain complete penetration from one side only. Complete penetration is also of prime importance on equipment used for elevated temperature service. Carbon deposits can build up in crevices and can generate enough force to actually break sound but incomplete welds.

A joint efficiency of 90% is usual for the high-nickel alloys when weld metal of the same nominal composition as the base material is employed. If the age-hardenable alloys, "K" Monel and Inconel "X," are welded with electrodes of the same family of alloys, but of the nonage-hardenable variety, the resulting weld will respond somewhat to age hardening, but not to the extent that the base material responds. Consequently, a lower joint efficiency will result. Table 6 illustrates some of the mechanical properties of welds in high-nickel alloys.

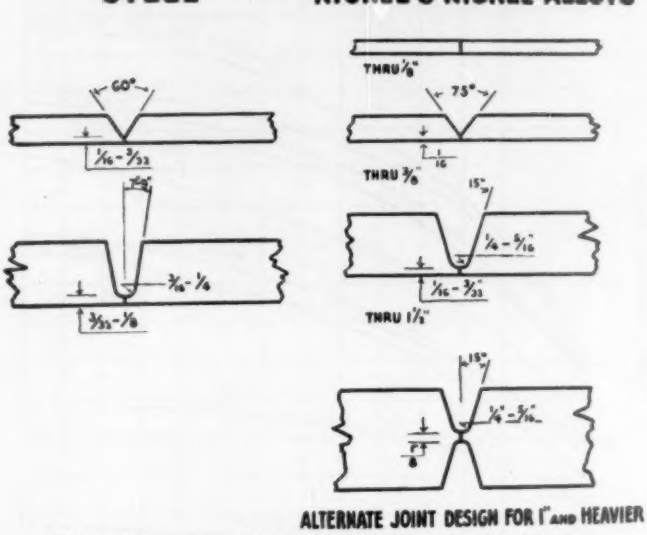

Fig. 4 Difference in design of butt joint for steel and nickel alloys

The procedure for welding high-nickel alloys is much like that employed for steel. A welding speed approximately 15% slower than for steel will be required for the high-nickel alloys. A shorter arc length, and somewhat more manipulation of electrode will be required. Figure 5 shows the electrode positions employed for a number of joints. All electrodes are designed for use on direct-current reverse polarity (electrode positive). Inconel and 80/20 nickel-chromium electrodes may also be used on alternating current. Arc blow is encountered sometimes with the magnetic alloys, nickel and low-carbon nickel, but is not a factor with the non-magnetic alloys. Spatter is generally absent when welding on the high-nickel alloys. Excessive spatter is a sign that a procedure factor is wrong such as too long an arc, too much current, or the wrong current or polarity.

Table 6—Mechanical Properties of Welds in Nickel and High-Nickel Alloys

	Minimum tensile strength	*Minimum yield strength (0.2% offset)*	*Elongation, %, in 2 in. (min.)*
Monel	75,000	...	30
"K" Monel	92,000§	45,000	30
	130,000*	90,000	10
	150,000†	105,000	10
Nickel	72,000	...	30
Inconel	85,000	...	30
Inconel "X"‡	105,000§	60,000§	30§
	120,000¶	74,500¶	25¶
	163,800*,¶	116,000*,¶	23*,¶

* Age hardened.
† Hot-rolled—welded and aged (values at −300° F.)
‡ Actual test values.
§ As-welded.
* Welded with exp. inconel "X" electrode.
¶ Aged, 1300° F. −20 hr.

The preferred procedure for welding clad material is shown in Fig. 6. The steel side is first prepared in such a manner that the first pass of steel weld metal will penetrate close to but not into the cladding material. If the first steel pass is allowed to penetrate into the cladding, a hard and brittle weld deposit will result and cracking can be expected. The steel weld is completed in a conventional manner. The low-hydrogen class of electrodes is preferred. The clad side of the joint is now prepared with a round-nose chisel or a grinding wheel to a depth which will remove all of the unfused crevice. It is important that all of the unfused area be removed. The clad side is now welded, preferably with at least three passes, one in the root and two cover beads. Iron pickup in the weld metal can be as high as 30-35% in the first pass, and will drop to 12-15% in the second pass and to approximately 5% in a third pass if one is used. The amount of iron pickup is somewhat under the control of the welding operator. If he uses high current and plays the arc on the steel base material, a maximum iron pickup will result. However, if he uses the proper current and dissipates the force of the arc against the molten puddle already deposited, a minimum iron pickup will result. On nickel-clad material, the nickel side is welded generally with a nickel electrode. Where the nickel cladding is 0.050 in. or less, an 80/20 nickel-chromium electrode is employed, particularly where iron pickup in the weld may have a deleterious effect on the use of the equipment. Chromium in the weld deposit permits the

presence of more iron without harmful effects. The Monel side of Monel-clad steel is welded with a Monel electrode specially developed for this purpose. This electrode is also recommended for overlaying Monel on steel and for joining Monel to steel. With the special Monel electrode it is not necessary to use an underpass of nickel as required when the usual Monel electrode is employed. The Inconel side of Inconel-clad steel is welded with 80/20 nickel-chromium electrode. Iron dilution in this weld will bring the deposited weld metal to approximately the Inconel composition.

The high-nickel alloys may be joined to steel, stainless steel, copper-nickel alloys and other materials quite readily. Inconel or 80/20 nickel-chromium electrodes may be used for any of the combinations mentioned. The special Monel electrode already mentioned may be used to join Monel and nickel to steel and stainless steels. This electrode also has an interesting application in joining the new 9% nickel steel for use at subzero temperatures. Nickel electrodes may be used to join nickel to steel or stainless but the results are somewhat variable depending on the amount of dilution obtained.

Metal-arc welding is the preferred process for installing linings of high-nickel alloy sheet or strip to new or existing vessels. It is vitally important that strict attention to cleanliness be observed on all lining jobs and particularly in applying a lining to existing structures. Figure 2 was an illustration of difficulties encountered because cleanliness was not observed.

The thickness of lining is dictated by both the intended service life and the ease of installation. A minimum of 0.062 in. is required for floors and walls, while 0.078 in. minimum is required for ceilings where overhead welding will be required. Thinner gages can be employed but the cost will be increased because of the difficulty of welding.

The width of sheet or strip is dictated by the service requirements of the vessel. Where vacuum conditions will prevail or where the service conditions are relatively severe, narrow strips (4 in.) are employed. If the service requirements are not severe widths up to 18 or 20 in. can be employed.

The attachment of individual strips or sheets to the vessel wall may be done by any of the four procedures shown in Fig. 13. It will be noted that each procedure attaches the lining only at the edges. Plug welds have been used in the past but are not recommended because plug welds are a constant source of difficulty. The dilution at a plug weld is at a maximum and the area is usually highly stressed. These conditions promote cracking.

Procedure A in Fig. 7 is recommended wherever possible because the fit-up of sheets is much easier than any other method and because no dilution of the sealing weld is encountered if the arc is not allowed to penetrate the first sheet. The attachment weld may be intermittent or continuous. A continuous attachment weld is more expensive but permits the pressure testing of each individual sheet.

Where a flush lining is desired, Procedure D is recommended. Here a continuous attachment weld joins the first sheet to the vessel. This weld is thoroughly cleaned and all bumps and irregularities ground or chipped off. The second sheet is now brought up to the first sheet and joined by welding against the first attachment weld.

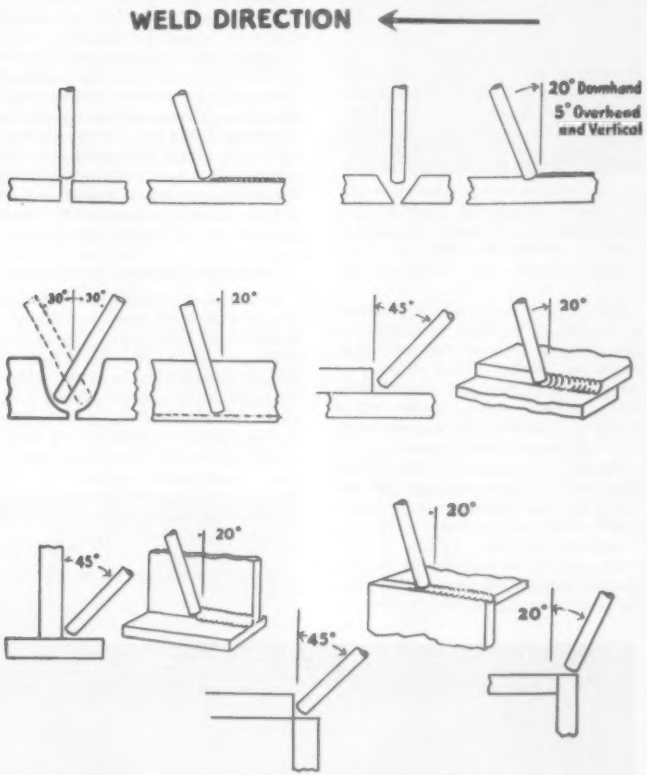

Fig. 5 Electrode positions for welding nickel and high-nickel alloys

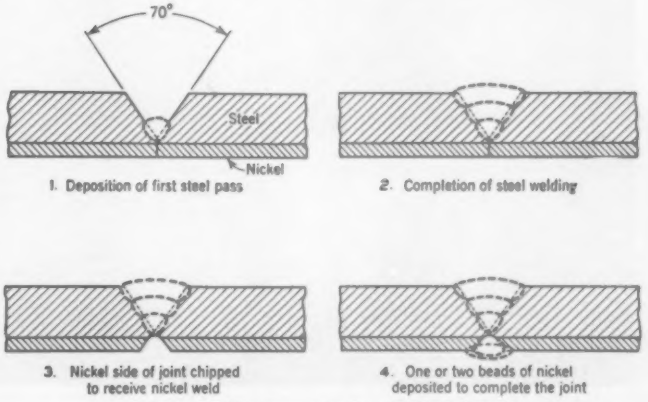
70°
Steel
Nickel
1. Deposition of first steel pass
2. Completion of steel welding
3. Nickel side of joint chipped to receive nickel weld
4. One or two beads of nickel deposited to complete the joint

Fig. 6 Welding sequence for nickel-clad steel

Procedures B and C have been employed successfully but require a high degree of precision in fit-up to control the amount of dilution.

Inspection procedures for high-nickel alloy weldments are very similar to those employed for inspecting steel weldments, except that magnetic particle inspection is not particularly useful. Radiographic standards of steel may be employed. The usual jig bend or free bend tests for qualification are employed with nickel and the high-nickel alloys. Sometimes these tests are made on specimens in which the weld runs longitudinal instead of transverse. Such a specimen bends all three materials, the base plate, the weld and the heat-affected area an equal amount. In some instances where transverse weld specimens are employed, the heat-affected area may be softer and weaker than either the weld metal or the plate. An elongation of 50% in the heat-affected area may be attained but failure in the heat-affected area occurs before the weld elongates appreciably. Thus the elongation in a gage length which includes weld, heat-affected area and base material may be below the desired amount.

Because the high-nickel alloys are so often used for their corrosion-resisting properties, particular attention must be paid during inspection to details which will affect this corrosion resistance. Backing strips, incompletely fused areas, porosity and cracked welds are sources of crevice corrosion. Porosity is not particularly dangerous unless it appears at or near the surface.

INERT-GAS METAL-ARC WELDING

The use of this process continues to gain in popularity and many applications of it have been made on thin sheet material in the high-nickel alloys. Either helium or argon of high purity can be used as a shielding gas and either d.-c. straight polarity or a. c. may be used as a source of power. Direct current is generally preferred.

The greatest source of difficulty occurs in the formation of porosity. Porosity is very pronounced if the gas atmosphere is impure, either because the helium or argon is impure to start with or becomes impure due to leakage of air around the torch, or by the induction of air under the cup. The arc length is fairly critical with nickel and Monel. An attempt should be made to maintain the arc length as short as possible. Arc length should not exceed 0.100 in. for argon and 0.200 in. for helium.

Special filler wires have been developed in the high-nickel alloys specifically for this process. They are a great help in controlling porosity. These special filler wires should be employed wherever filler wire is necessary and in addition will be helpful if employed even in cases where filler wire is not normally required.

Figure 8 shows cross sections of welds made with inert-gas metal-arc welding. Figure 9 is an excellent example of the results which can be obtained.

Inert-gas metal-arc welding is normally applied to material $^1/_8$ in. and thinner. Over $^1/_8$ in. the metal-arc process may prove to be more economical, however this picture may change rapidly as more experience is obtained with the automatic inert-gas metal-arc process using a consumable electrode (Aircomatic and others). Some very interesting results have already been obtained experimentally using this latter process on clad material.

Inspection of welds made by the inert-gas metal-arc process should not be made solely by visual examination. Gross porosity, when it occurs because of impure atmosphere, can be completely contained within the weld and two perfect surface skins (top and bottom) tend to create a delusion of a good weld. Sectioning, etching and examining under low-power magnification, or notching and tensile testing a transverse specimen are good methods of test. X-ray examination will disclose gross porosity but not fine porosity.

SUBMERGED-ARC WELDING

Submerged-arc welding has been used to join high-nickel alloys to themselves in thicknesses $^1/_8$ in. and up. Special filler alloys are available for this process but the only fluxes or melts available are those employed on steel and other alloys. Under good quality control conditions satisfactory results can be obtained with Monel, nickel, and Inconel. This process has not been satisfactorily applied to "K" Monel and Inconel "X" primarily because the arc under the flux blanket changes the chemistry of the weld deposit.

ATOMIC HYDROGEN WELDING

This process has fewer applications

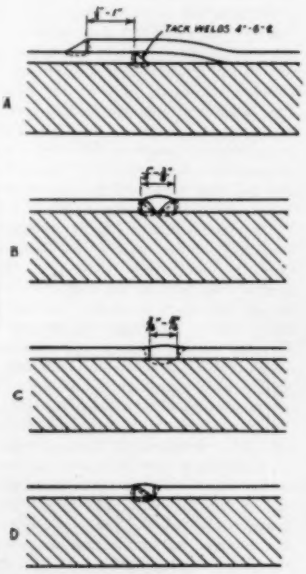

Fig. 7 Applied lining joints

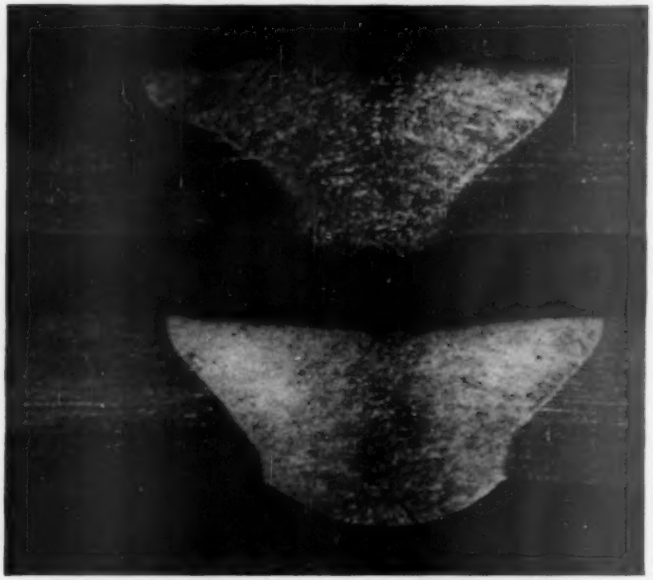

Fig. 8 Cross section of welds made with inert-gas metal-arc process

since the introduction of the inert-gas metal-arc process. In the past the atomic hydrogen process has found its main application in automatic setups where the rate of weld-puddle solidification could be kept under control. Molten nickel will absorb large quantities of hydrogen which are released as the metal solidifies. Porosity can occur if the rate of solidification is too rapid.

OXYACETYLENE WELDING

Oxyacetylene welding is employed on material $^1/_8$ in. and thinner and on pipe or tubing 2 in. O.D. and under regardless of wall thickness. A neutral to slightly reducing flame is employed ($^1/_{16}$-in. feather of excess acetylene). Too much acetylene may introduce carbon into the weld thus lowering its properties. An oxidizing flame and particularly one that fluctuates from oxidizing to reducing will produce a very sluggish puddle and a brittle weld.

No flux is required when welding nickel or low-carbon nickel but boron-free fluxes are required for all the other alloys. Boron in the flux makes the weld metal very fluid but it forms a low melting-point alloy which flows through very tight crevices. This alloy is brittle and may result in weld failures. Figure 10 shows an Inconel lap joint. Note that the top figure in which a boron-containing flux was employed shows a thin flowing alloy running between the sheets. This situation is not present in the bottom figure where a boron-free flux was employed.

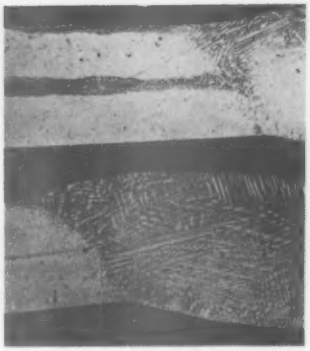

Fig. 10 Top—Inconel oxyacetylene welded with high-boron flux. Bottom —Inconel oxyacetylene welded with boron-free flux

The top and bottom of all joints as well as the filler rod should be fluxed. The torch should be held so that the tip of the cone just touches the surface of the puddle but agitation or "puddling" should be avoided.

OXYACETYLENE PRESSURE WELDING

This process has found limited application in joining high-nickel alloys to themselves. Figure 11 shows the cross section of a Monel pressure weld.

The preparation for welding requires a flat finely machined or ground surface on the abutting surfaces. These surfaces, in addition, must be clean and free of oil, grease, oxides and other foreign substances. On Monel, nickel and Inconel, no further preparation is required. However, on "K" Monel and Inconel "X" it appears that the refractory oxides formed by aluminum in the case of "K" Monel and aluminum, titanium and silicon in the case of Inconel "X," are not readily dispersed in the welding process and result in a weld of low strength. It is possible that a flash of nickel plating may overcome this difficulty, but this procedure has not been attempted.

RESISTANCE WELDING

The resistance welding processes, spot, seam, projection and flash welding are employed readily on nickel and the high-nickel alloys. The new pamphlet "Recommended Practices for Resistance Welding" C-1.1-50, published separately by the American Welding Society and included in the Handbook contains rather complete information for spot welding Monel, nickel and Inconel to themselves in dissimilar thicknesses from 0.005 through 0.125 in. These alloys require a higher forging pressure than steel because of their generally higher mechanical properties. The age-hardenable alloys "K" Monel and Inconel "X" should be welded in the soft condition. The tensile shear strengths of resistance welds in these alloys will be increased approximately 50% by the aging treatment.

Seam welding of the high-nickel alloys is readily accomplished but factual data for all thicknesses is not yet available.

Inspection of spot, seam and projection welds in the high-nickel alloys is similar to requirements for steel. The relatively high strength of marginal welds (stuck welds) in the high-nickel alloys makes the use of the peel test inadvisable. It is much more satisfactory and almost as fast to section, etch and examine the cross section either visually or under low power magnification.

Flash welding of the high-nickel alloys has been successful in many instances such as the making of chain, and the side seam on Monel hot water heaters. On more modern machines where the energy level can be changed during the welding cycle, it is not difficult to obtain a satisfactory machine setting where sufficient energy is available for the start of flashing and where this energy can be reduced during push-up to prevent interdendritic melting. On older type machines where such change of energy is not possible, the

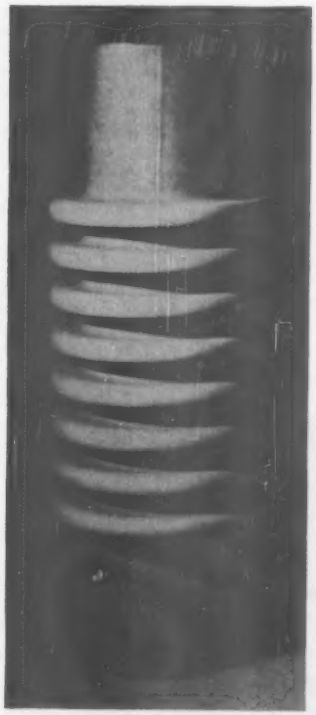

Fig. 9 Inconel expansion bellows welded with inert-gas metal-arc process and formed after welding

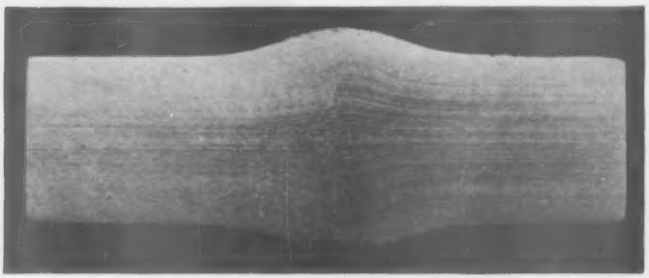

Fig. 11 Cross section of pressure weld in 1-in. diameter bar. Monel bar macro etched to show flow lines

time of current flow after push-up is quite critical.

Inspection of flash welds is best done by sectioning and etching to determine that all dendritic material has been extruded during push-up. Mechanical testing of flash welded joints is preferred over X-ray or other forms of nondestructive testing.

SILVER BRAZING

The high-nickel alloys may be joined with silver-brazing alloys where the corrosive environment will permit the use of such alloys. Alloys containing phosphorous should never be used on high-nickel alloys since they will cause embrittlement in much the same manner as they do on steel. Any silver-brazing alloy not containing phosphorus, lead or bismuth may be employed for brazing annealed high-nickel alloys. Stress cracking in the presence of molten silver-brazing alloys sometimes occurs with the high-nickel alloys as it does with other high-strength and high-annealing temperature alloys. Stress to cause cracking may occur from cold work in the material, or from stress applied during the brazing or may result from thermal stresses set up by very rapid heating. If stress cracking does occur, examine the procedure to determine where the stress originates and remove this source of stress. In some instances it is reported that silver-brazing alloys not containing cadmium are less susceptible to stress cracking.

The usual requirements for any silver-brazing application are applicable to high-nickel alloys. The fit of parts should be controlled to obtain approximately a 0.003-in. gap. The parts must be clean. Heat may be applied in any of the conventional methods. Silver-brazing alloys lose their strength rapidly as the application temperature is raised and are not useful over 700° F. Even the 85/15 silver-manganese alloy is not useful above 1000° F.

Figure 12 shows an interesting example of silver brazing on a high-nickel alloy.

BRONZE BRAZING

Bronze brazing or welding can be used on high-nickel alloys but is seldom employed. It is usually preferable to weld and eliminate the dissimilar metals combination which might cause corrosion difficulties.

COPPER BRAZING

Since the high-nickel alloys have high melting points, they may be furnace brazed using copper or a copper alloy. Molten copper will alloy very rapidly with nickel or Monel, consequently it cannot be made to flow quite as readily as it does on steel. The copper-brazing alloy should be placed in or closely adjacent to the joint. Figure 13 shows one application where the copper is plated on strip prior to coiling and furnace brazing.

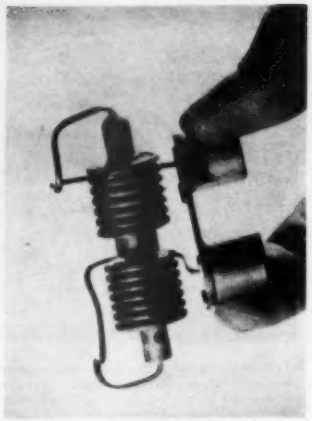

Fig. 12 Assembly of Inconel bellows for fuel pressure gage manufactured by Ranco Inc. 30 Silver-brazed joints must withstand 750 psi. test pressure

Alloys containing chromium, aluminum and titanium are more difficult to copper braze since oxides of these metals are not reducible in the usual furnace atmosphere. Copper plating of these alloys prior to brazing will prevent the formation of oxides which cannot be reduced. Chromium oxide can be reduced if the atmosphere of the furnace is sufficiently dry but obtaining an atmosphere with a dew point of −50 to −100° F. is troublesome and costly.

If cracked city gas is employed as an atmosphere it is imperative that the sulphur content be low otherwise severe embrittlement will occur at the 2050° F. furnace temperature.

Copper-brazed joints have poor resistance to corrosion when the temperature exceeds 1000° F. and the joint strength is very low above this temperature.

BRAZING FOR HIGH-TEMPERATURE SERVICE

One of the newcomers in the brazing field is a nickel-chromium-iron-boron-silicon alloy commercially available as Nicrobraz.*

This alloy was originally developed as a hard-surfacing alloy and has a deposited hardness of 58–62 Rc. Its use as a brazing alloy is very recent. The furnace atmosphere must be dry hydrogen with a dew point of −100° F. The alloy is very fluid at 1950–2000° F. and will flow relatively long distances. It is employed to join the high-nickel alloys, the stainless steels and other so-called super alloys used in high-temperature applications. The corrosion resistance of this alloy at elevated temperature is excellent and far exceeds copper or silver alloys. Furthermore, its high-temperature strength closely matches the strength of the base material. During brazing the boron diffuses into the base material and the alloy's melting temperature rises with the result that the joint cannot be unbrazed at the original brazing temperature. Successful application of this alloy at service temperatures of 1800 and 2000° F. have been made.

THERMAL CUTTING

The high-nickel alloys cannot be cut by the conventional oxyacetylene cutting equipment. Clad plate can be cut if the cut is made from the steel side.

Powder cutting, in which finely divided iron powder is injected into the cutting-oxygen stream and burns producing an extremely hot and abrasive stream will cut all the high-nickel alloys. Oxyarc cutting employing a flux-covered tubular electrode through which oxygen is forced under pressure will also cut high-nickel alloys. A carbon arc or a steel electrode with high amperage may also be used in limited applications to melt out shapes.

All of these processes tend to contaminate the cut edge with iron. However, the depth of this contamination is not great. Any cut surface must be ground or machined in order to remove this contamination prior to welding or other operations. If all signs of discoloration are removed the corrosion resistance of the base material will be unaffected by the cutting operation.

* Wall Colmonoy Co., Detroit, Mich.

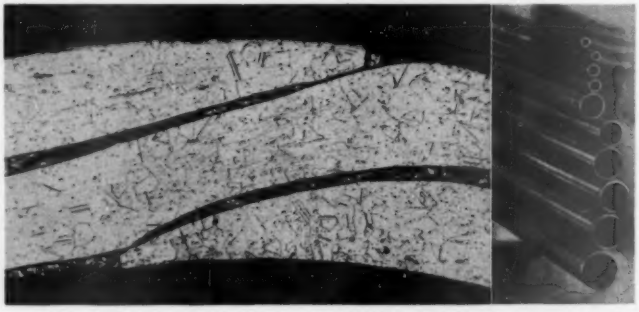

Fig. 13 Left—Section of typical tube at lap. Right—Monel bundyweld tubing fabricated by copper brazing

Powder cutting gives the best quality of cut in both manual and pantograph setups. Oxyarc cutting is limited to manual operation and the quality of cut is greatly influenced by the skill of the operator. The other two processes are generally limited to field operations of an emergency nature and the quality of cut is usually rather poor.

A process commonly used for cutting stainless steels in which a flux is injected into the cutting oxygen stream, is not applicable on high-nickel alloys since it will not cut alloys containing over 30% nickel.

JOINTS IN DISSIMILAR METALS

There are many designs which call for joining one high-nickel alloy to another high-nickel alloy or to steels, stainless steels or other materials. Practically any combination of metals can be joined by one or more of the processes discussed but all processes are not foolproof in application. Some combinations of dissimilar metals when melted together in a weld can produce alloys with very undesirable properties. Such combinations can be difficult to weld without cracking or without producing a joint with poor mechanical properties. Other combinations of materials produce welds with intermediate mechanical properties between those of the base materials being joined. Such combinations are not at all difficult to weld.

Wherever one of the brazing processes can be tolerated by the service condition, brazing offers a simple solution to this joining problem since neither of the base materials is melted. Flash welding offers a similar solution to the problem since almost all of the admixed metal is extruded from the weld in a properly made flash weld.

This simple approach to the problem is not always possible when processes such as metal arc, oxyacetylene, inert-gas metal arc, spot and seam welding are to be employed, but nevertheless some solution can usually be found.

In fusion-welding processes where a filler metal can be added, one solution is to employ a filler metal which is compatible with both of the base materials to be jointed. The special Monel electrode already mentioned in conjunction with welding Monel-clad steel, can be used to join a copper-containing alloy such as Monel, or Cupro-nickel to other high-nickel alloys or to steel. Too much dilution of base metal in the weld can cause difficulty when welding on steel or stainless steel but reasonable care will permit a successful application. One interesting fact is that the Inconel or 80/20 nickel-chromium electrode seems to be the most universal in making dissimilar joints between a wide variety of ferrous, nickel-base and copper-base alloys. The Nickel electrode, the stainless-steel electrodes and the regular Monel electrode can be used for some specific applications but they are by no means as universal in application.

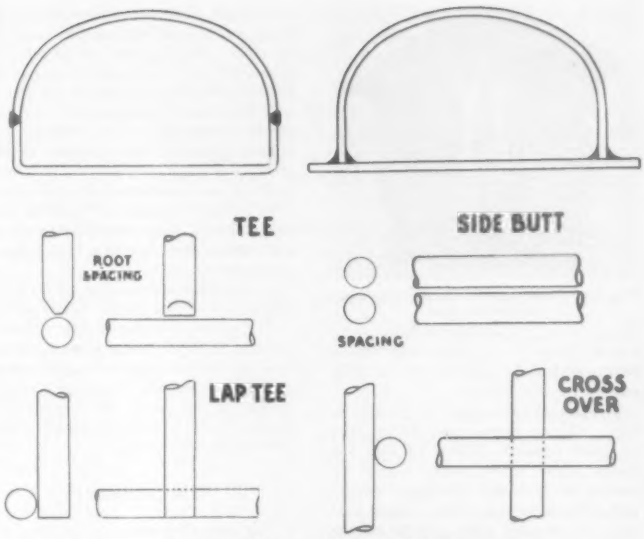

Fig. 14 High-temperature fabrication

It is best to avoid the oxyacetylene process and the inert-gas metal-arc process when it is known that an alloy of poor mechanical properties will result in the weld. It is not always possible to control penetration and dilution by adding a compatible filler material with these processes.

Spot and seam welding of dissimilar metals can be difficult too. Joining Monel to steel with conventional equipment will result in cracking. One solution is to employ a procedure which will cause melting to occur on only one metal (the Monel), and a braze-type weld results without intermixing of the two materials. It has been reported that low-inertia machines and slope control or its equivalent can produce adequate forging to permit the welding of this combination without cracking.

HIGH-TEMPERATURE APPLICATIONS

There is still a considerable amount of information to be developed on the fabrication of muffles and other equipment for uses at temperatures up to 2100° F., but in the work that has been done to date, three basic rules have evolved which are applicable to all high-temperature fabrication. The rules and explanations of their use are shown in Fig. 14. The first rule is: the weld must be all the way through or all the way around. Wherever possible, as in butt joints, in heavy sheet or plate, the joints should be prepared so that the weld penetration can be complete. Items, such as annealing baskets, made up of round, flat or square stock require that the welds be all the way around. That is, where two members abutt or cross, the weld must make a closed joint to prevent the formation of carbon between the contacting members. This carbon accumulation can create sufficient pressure to cause failure at elevated temperatures.

Beveling the butting ends of square or round stock will be required in many instances. Where this is required, the bevel

Fig. 15 Molten flux attack on 1/4-in. Inconel arc-welded pipe. Lower end exposed to 2100° F., upper end cool

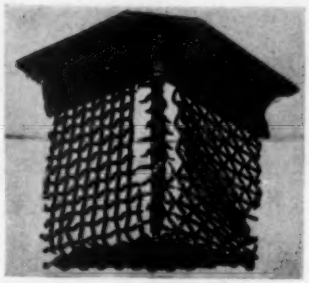

Fig. 16 Corrosion of Inconel by oxy-acetylene flux

should be to a chisel edge rather than to a rounded point. A chisel edge allows better control of heat input and weld contour.

The second rule applies to joint location. Corner joints must be avoided wherever possible. There are very few places where corner joints cannot be avoided, even though this might require redesign in some cases. If a corner joint cannot be avoided make sure the weld is completely penetrated and reinforced. Wherever possible apply a fillet to the inside of the angle.

The third, but by no means the least of these rules is: all welding flux must be removed. Welding flux on nickel and high-nickel alloys will cause corrosion or damage to the base material if the unit is used at service temperatures above the remelt temperature of the welding slag. Figures 15 and 16 are illustrative of the effects that might be expected. This temperature varies rather widely with differences in flux composition. The rules outlined apply to other heat-resisting alloys and stainless steels as well as to nickel and high-nickel alloys.

Certain types of units intended for high-temperature service will give better service life if a sufficient number of loosely riveted joints are used to provide some flexibility. This type of construction is particularly useful for annealing crates or fixtures. Welding is very useful for fabrication of the frames and riveting for joining of support members or bottoms.

CONCLUSIONS

This lecture has attempted to cover the principal high-nickel alloys and their behavior during fabrication by welding. There is one thought that I would like to leave with you. The welding of high-nickel alloys is not difficult. Any structure that can be made from any other material, can also be made of a high-nickel alloy, and usually by the same process. There will be slight variations in procedure required by the special characteristics of the high-nickel alloys. The greater the understanding of the characteristics of the high-nickel alloys, the less will be the difficulties encountered.

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(Continued from page 228)

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Suggested New Welding Standards

Discussion by David Arnott

It can properly be said of the author, whether one agrees with him or not, that his views are always interesting. That simplification of welding standards and elimination of unessential testing are eminently desirable in the best interests of manufacturers and users alike goes without saying, but whether this can be achieved without some loss of efficiency by the simple tests suggested by the author is open to question. I have no expert knowledge of electrode specifications, never having served on the A.W.S. Filler Metal Committee which is thoroughly representative of all the various interests including the manufacturers of electrodes, but I am rather afraid that the problem is not quite so simple as Mr. Lincoln would lead us to believe. I cannot quite follow him when he compares a riveted and welded joint in terms of relative ductility, porosity, appearance, etc., and there is surely nothing analogous between an undercut in a weld and the crease which results from caulking the edge of a plate. To conclude that because a testpiece cut from a welded plate will invariably break in the plate rather than in the weld, and that, as some of us used to think, even an indifferent weld is better than the best riveted joint, is an oversimplification of the problem which is not one of relative joint efficiencies but rather the comparative efficiency of the structure as a whole after the component parts have been joined together.

Judging from Mr. Lincoln's remarks one would think that there was a conspiracy afoot to unduly emphasize the failures which have occurred to welded ships and to minimize similar happenings to riveted ships. This is simply not true. Riveted ships have admittedly failed in service, even to the extent of breaking in two, but such occurrences have been very few and far between, and in every such case the failure could properly be attributed to some specific reason or combination of causes such as overloading, badly distributed load, or abnormally bad weather conditions. Again, those riveted ships were invariably old where the scantlings, i.e., plate thicknesses, etc., had deteriorated as a result of corrosion to an extent that the effective material was insufficient to meet the stresses due to some extraordinary service condition. In the course of my shipbuilding experience of over 50 years I have never heard of a riveted ship which broke in two while lying at the Builder's Outfitting Dock prior to delivery, and such a contingency is almost inconceivable. On the other hand, serious structural troubles have occurred to welded ships during process of construction and before entering service. Having been an active proponent of ship welding for many years it cannot be said that I hold any brief for riveted construction. The policy of welding the ships built by the Maritime Commission during the war was a sound one and amply justified, if only in view of the tremendous volume of ships which our builders were able to turn out so expeditiously at a time when they were so badly required for the war effort. If those ships had been riveted we would undoubtedly have had some trouble with slack and leaky rivets which would have necessitated the ships being drydocked for rivet renewals, if our experience with the riveted ships built during World War I under the same emergency conditions is any criterion. I have no hesitation, however, in saying that we would have had none of the complete and spectacular failures which occurred to some of our welded ships built during the last war and which, to me at least, were entirely unanticipated. Mr. Lincoln attributes these failures to poor design. It is true that some of the design details in the earlier ships were unsatisfactory, and it is also the case that the steps taken to improve such details in existing and succeeding ships were highly effective in reducing the percentage of these casualties, but poor design is not, by any means, the whole story. It might interest the author of this paper to know that in most of these failures the crack originated in a weld although it did spread to the plate very rapidly. These failures have been carefully analyzed and all aspects of the problem investigated including such possible contributing factors as design, material and workmanship. Literally millions of dollars have been spent, both at home and abroad, in carrying out innumerable research projects initiated as a result of these welded ship failures some of which, especially those relating to material, have been very valuable, but the end is by no means in sight.

From the point of view of a proponent of welding the present position with regard to the welding of these large plate and shape structures cannot and should not be considered satisfactory. So far at least as ship construction is concerned most of the larger ships building today are having some riveting worked into their structure. Many of the problems relating to the structural efficiency of large welded ship structures still remain to be solved and their complete solution will not be facilitated by the oversimplification and apparent refusal to face the facts evidenced by this paper.

David Arnott is a Consultant to the American Bureau of Shipping., New York, N.Y.

Paper by **James F. Lincoln** was published in the September 1950 issue of THE WELDING JOURNAL, pp. 715–717.

Composite Metal Fabrication By Arc Welding

Some examples of surfacing for bearing, resistance to galling, and resistance to corrosion

H. E. Cable

BECAUSE of the difficulty of accurately conveying ideas between people, it is my opinion that semantics (the science of word meanings) should be of interest to everyone, engineers especially. It is fitting that the title of this session should have been changed to "Surfacing" because, while most such operations are "Hard Surfacing," we find more and more applications for softer metals for bearing surfaces, resistance to galling and resistance to corrosion.

"Surfacing" is the commonest form of "Composite Metal Fabrication" and several phases of this operation have been discussed by the preceding papers this morning and numerous articles which have appeared in the trade journals during the past several years.

Three years ago at this meeting I predicted widespread development and use in production fabrication of automatic arc weld surfacing using alloy wires, fabricated electrodes and flux-fed alloys which has since been widely applied and discussed. It is pleasing to watch such development progress because this process represents, in my opinion, one direction in which arc welding can accomplish the greatest technological and economic benefits. It is my purpose not to talk about specific alloys which has been done but to attract attention to still greater possibilities for economic progress through the extended use of this arc weld surfacing and other applications of the electric arc in its conventional or well-known forms and in newer forms for joining unlike alloys or dissimilar metals both ferrous and nonferrous.

To summarize briefly some established results of composite metal fabrication by arc welding, let us look at the following examples:

Mine Locomotive Wheel Rebuilding: One of the earliest and most successful operations using the submerged arc for mine locomotive wheel rebuilding has been in operation for about six years, during which time a steady, marked improvement in wheel wear has been evidenced. There are now at least 17 steadily operated automatic wheel rebuilding operations in the eastern coal fields and many of these have been inspected by visitors from all parts of the World.

Fig. 1 Application of a high-speed tool steel surface to these uncoiler cone adapters improved useful wear life almost fifty times

Mine Car Wheels: Of the newer types wherein the bore is an antifriction bearing race are being rebuilt at substantial economies over replacement cost.

The possibilities for substantial economies in the operation of steel wheeled equipment through the application of this rebuilding process are obviously *enormous*.

Steel mill maintenance has pointed the way to use of the arc for composite fabrication in many new products where the service records of rebuilt equipment have amply demonstrated the value of the composite structure. Among such products are crane wheels, Diesel switcher wheels, other car wheels, pump casings and parts, furnace skid pipes, coiler rolls, table rolls, journal surfaces and uncoiler cone adapters. (See Fig. 1.)

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Paper presented at the Thirty-first Annual meeting, A. W. S., Chicago, Illinois, week of October 22, 1950.

Fig. 2 This mill hammer is completely fabricated by cutting from high-carbon plate, welding in the round bar stub shafts and surfacing with a chrome carbide type deposit

The mining, earth-moving and bulk materials handling industries are the obvious fields for surfacing and their experience has likewise contributed to the use of composite metal fabrication in new equipment for these fields such as steel flanged wheels as above, digging points, pump casings and parts, tractor rollers, scraper blades, plowshares and drill pipe wear rings. (See Figs. 2 and 3.)

In miscellaneous manufacturing and commerce, many factors other than simple abrasive wear are encountered and these have responded to composite metal fabrication such as gear wheels, sprocket wheels, trunion rings, sheaves and cable drums, small electric generator frames having a better combination of strength with magnetic permeability, dryer kilns and stoker screws with high temperature alloys on the hot end, dryer rolls, calender rolls, milling cutters and the outstanding example of the gas turbines. Even at home the lawn mower has been made lighter and better by arc welding and composite alloy fabrication. (See Figs. 4 to 9.)

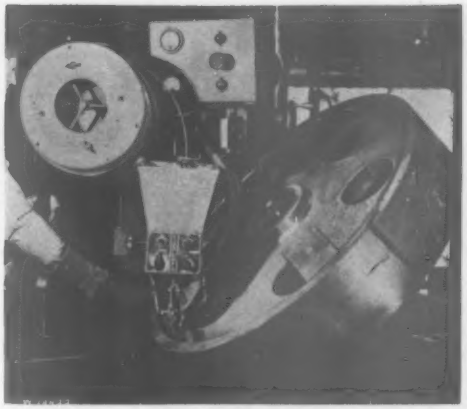

Fig. 4 The use of high-grade alloys for the rim in which gear teeth are cut and the hub carrying the keyway is today common practice in the fabrication of gear blanks

Composite metals as a commodity are, of course, no novelty though the extent of their use and the range of the available types is constantly expanding. Bi-metal strips were not arc welded generally but the arc is widely used either directly or incidentally in the production of the well-known clad products such as Allegheny-Ludlum's Pluramelt, Composite's Sandwich,

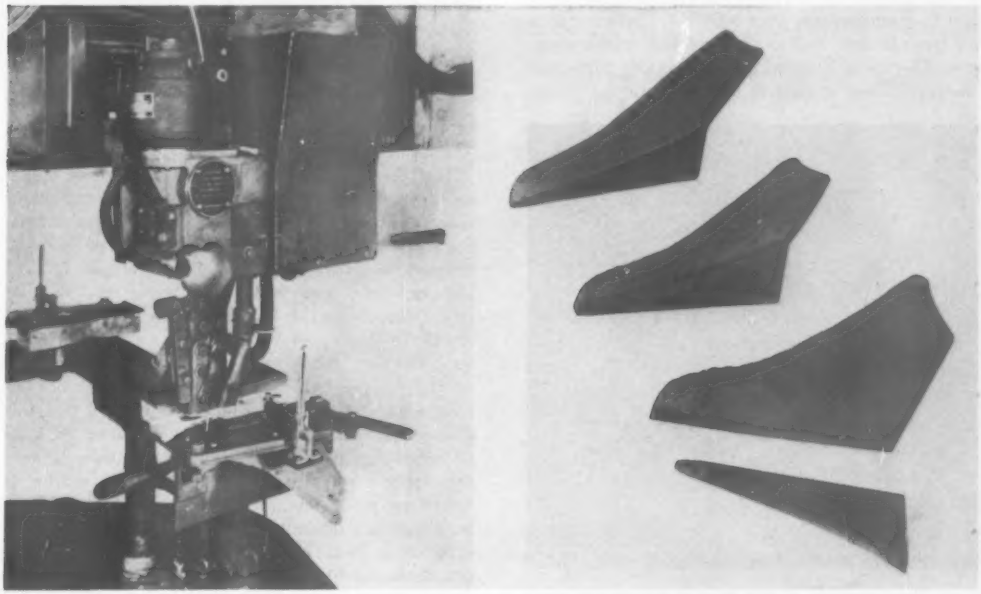

Fig. 3 The composite nature of plow points is much more economically done and the product gives improved service through the use of submerged arc welding

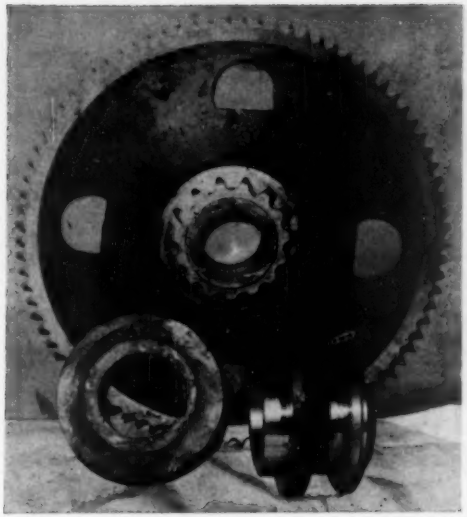

Fig. 5 The stress on the teeth of the small sprocket is much greater than on the larger sprocket due to the obvious difference in lever arm, and the use of high-strength steel in the smaller sprocket represents substantial economy in the cost of the whole piece

Superior's Su-Veneer and American Cladmetal. Saw blades having a high-speed steel cutting edge with a tough alloy backing are widely used.

Again the matter of semantics creeps into the picture when we consider joining ferrous and nonferrous metals but I guess they will all truly be welds because any application involving the electric arc is pretty sure to be raised to a temperature over 800° F. These might in some cases be described as "braze welds" where a nonferrous filler metal is added though this also might lead to some confusion in some of the applications.

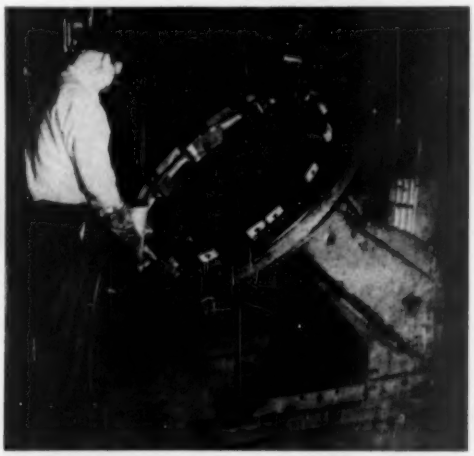

Fig. 6 The tire or rim of this trunion ring carries heavy loads and its service life is greatly improved by the use of flame-hardenable SAE-1045 steel

Fig. 7 Improving the wearing qualities of the sheave improved the service life of the much more costly steel cables. A flame-hardenable rim on a mild-steel webbed structure gives a very economical and highly satisfactory piece

Copper, stainless, tungsten and highly alloyed steels are now very commonly joined by flash welding in the manufacture of a wide variety of electronic tubes. Lincolloy is fabricated by alloying metallic copper with steel in the arc; bronze-bearing surfaces are deposited on steel, both external and internal (see Fig. 10) copper in bar form is welded against steel using either nonferrous or ferrous electrodes. The transition layer of bronze will permit bonding aluminum to steel.

In manual shielded arc welding improvement of "low-hydrogen" coatings has given impetus to composite fabrications.

The use of the inert gases to shield the arc in simple and composite nonferrous and austenitic ferrous alloys is an important technique though of definite economic limitations. Certainly we should not overlook the truly sound progress being made in composite fabrications of steel castings with rolled steel.

Since composite fabrication of ferrous alloys is almost certain to involve alloys of limited weldability based upon the ultra fast quench and cooling of weld metal which is quite generally recognized though incompletely understood, we should point out also that this phenomena is not entirely of negative value. We are all acquainted with the facts that the failure of weld deposits under severe restraint is largely a matter of design and procedure and must remain so even though we are joining dissimilar alloys.

However, we are generally much less familiar with the superlative qualities of strength and ductility which are imparted to a wide variety of ferrous alloys by passing them across an adequately shielded arc into a well-

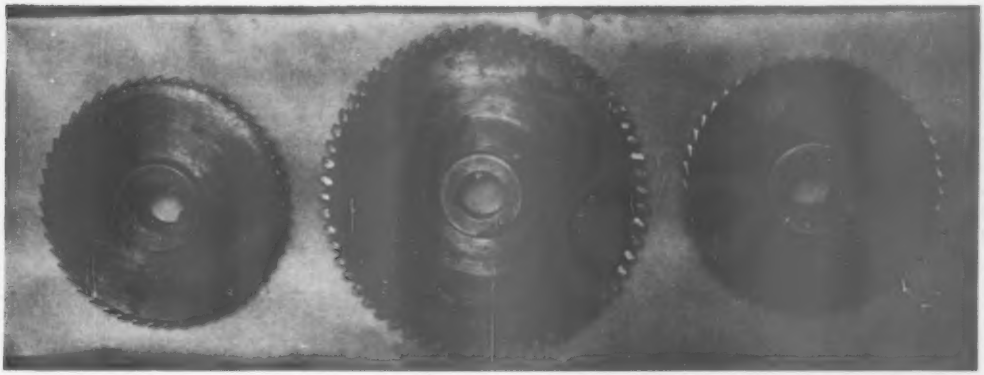

Fig. 8 High-speed milling cutters in which only the cutting surfaces are integrally welded high-speed tool steel. In this case the blanks are cut from 3% nickel steel and the cutting edges applied with the arc are of the moly-tungsten high-speed composition

fluxed pool. We are much less familar with the delayed, in terms of short but practical working intervals, reactions in structural transition in many of our common high-strength and special-purpose alloys which can be of great practical benefit in composite fabrication.

Our attention has been focused on these metallurgical phenomena through application of the high-density submerged arc process which has become rather generally known as "squirt welding." Because of the unique arc-force directional properties obtained with this process, it has been possible to join, by arc welding, pieces of dissimilar metals having very divergent properties of magnetic permeability, electrical and thermal conductivity. An outstanding example is the fillet welding of a copper bar against the surface of a mild steel plate using an ordinary mild steel electrode and a common flux composition. This extreme penetrating power with electrode positive is very successfully used for through-welding thin coating sheets to heavy structures.

Conversely the extremely high melt-off rate in proportion to arc current obtained with this high-density arc with electrode negative makes it possible to deposit a surface layer at higher speed and with less base metal admixture than has been possible with the conventional submerged-arc welding processes.

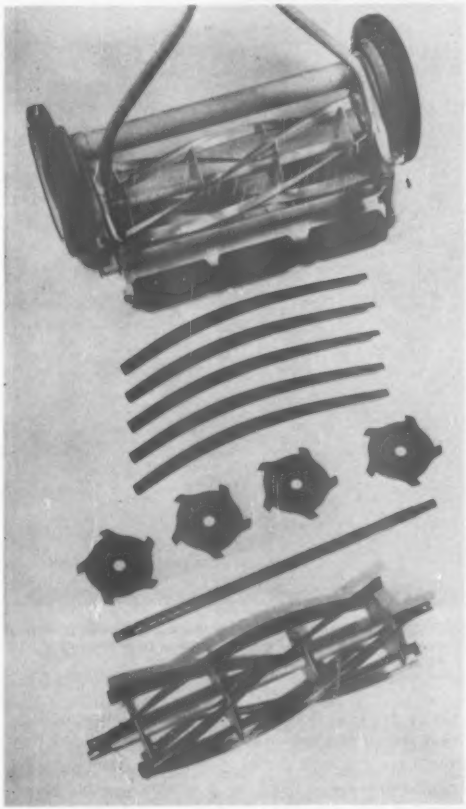

Fig. 9 Improved quality of lawn mower through the use of high-carbon cutter blades made integral with a tough, yet low-cost, spider structure through the use of the low-hydrogen shielded manual electrode

Fig. 10 The bronze-lined ring is welded into the already honed cylinder

Coincident with this high-density arc development, the development of a low cost and very flexible automatic wire electrode feeding device has permitted automatic surfacing applications in confined areas such as the bores of industrial equipment: sheaves, pulleys, castors and wheels. This surface becomes an integral part of the piece and it is interesting to note the contrast between depositing a bronze composition for a friction-bearing surface as against a highly hardenable ferrous alloy to provide an antifriction bearing race.

In these days when World supremacy of a way of life is in the balance, the tilt of that balance is going to be greatly affected by our ability to refine and apply metals to the greatest technological and economic advantage. The general use of the arc-welding processes will certainly increase and it seems to me that composite metal fabrication offers a tremendously important response to the dire need for conserving our less plentiful alloying elements by application of the truly functional concept, using the right amount of the right material in the right place which is so essential to good design for welding.

Flame Gouging for Better Welds in Autoclave Fabrication

FLAME gouging with the oxyacetylene flame plays an important part in the fabrication of autoclaves at the Blaw-Knox Co., Blawnox, Pa. This process provides an easily controlled means for quickly and accurately removing a narrow strip of metal from steel surfaces.

Figure 1 shows an autoclave used in the chemical processing industry with a shell, approximately 7 ft. in diameter, joined to a head and jacket bar by exterior welds. The bottom, or root, of the initial weld is cut away by oxyacetylene flame gouging in the interior of the tank (Fig. 2). This interior head seam is then further cleaned with a chipping hammer. Clean metal is assured for the final step of making the interior weld.

The body of the autoclave is made of carbon steel. The head is clad with Type 347 stainless steel on the interior.

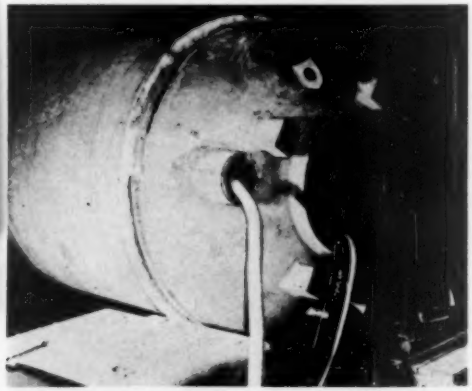

Fig. 1. Autoclave used by the chemical processing industry

Shell has been joined to the head and jacket bar by exterior welds

Photos and data courtesy The Linde Air Products Co.

Fig. 2. The bottom, or root, of the initial weld is being cut away by oxyacetylene flame gouging to prepare for the interior welds

Flame gouging differs from ordinary flame cutting in that the cutting action does not progress all the way through the metal material. A special flame-gouging nozzle is provided to deliver a relatively large jet of oxygen at low velocity. A cutting blowpipe is used, equipped with a No. 25 flame-gouging nozzle. The number "25" refers to the approximate diameter of the cutting oxygen orifice in hundredths of an in.

Arc Welding in Home Building

by John D. Bert

WELDING has gone places in the past 30 yr. Pressure vessels, piping, turbines, Diesel engine frames, ships, railway cars and airplanes are advantageously welded. But to what extent is it used in the building of homes? It is a big field; why have men with a knowledge of welding by-passed it? There have been a few tries at prefabricated steel homes, but of the eleven or twelve billion dollars spent in the past year for homes, welding has had an infinitesimal share. Why? I believe only about 65% of possible welding applications have been tried. I feel that if the housing dollar were really gone after, we could add 10% more to the now successfully welded products.

The greater part of our housing today is built by local contractors. Even Levitt Bros. of Long Island are local contractors on a streamlined, stepped up, modern scale. Assembly-line production, like that of automobiles, for instance, does not seem to suit the building of homes. The prefabricated field represents only a small percentage of houses built today. Family needs and activities differ and require different plans and room layouts to house these needs and accommodate these activities. At the same time, the great majority of people want houses not radically unconventional.

Contractors have overlooked the possibilities of welded steel construction. There is relatively very little actual welding necessary in building steel houses. A 5-room bungalow requires approximately 35 lb. of electrodes. Compared to the welding of high-pressure steam piping or Diesel-engine frames, there is very little skill required. Any mechanic, with some welding experience, a few hours of training, and using a small a.-c. welder, can weld a steel house if he follows a definite welding procedure and sequence.

To popularize welded steel construction, the writer feels that the welding must be done at the site, where individual ideas of prospective buyers can be carried out. What steel has to offer is sound construction at big savings over old methods, provided it is fabricated at the site, eliminating expensive plants and equipment, high-salaried executives and trained erection field crews.

John D. Bert, Fayetteville, Pa.

Fig. 1 Completed steel home

Figure 1 shows a steel house, fully equipped, welded at the site, the steel arriving from the warehouse in standard lengths and shapes. This 5-room bungalow can be built of steel, with all of its advantages, for less than $6000, not including the land. In this home, the living room measures 18 x 13 ft., the kitchen and dinette 15 x 10 ft., the master bedroom 12 x 12 ft., the second bedroom 10 x 10 ft., and the bath, which includes a tub and shower, 5 x 7 ft. (Fig. 2). A 4 x 7 ft. storage room also is provided, together with three other closets and a linen closet. The compact furnace room houses an electric hot-water heater and an oil-fired furnace.

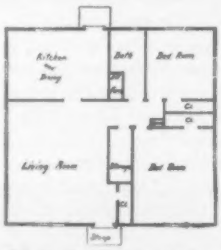

Fig. 2 Floor plan of sample steel home

Fig. 3 Welded steel shell showing steel framing steel outside walls, window and door frames all welded into one integral unit

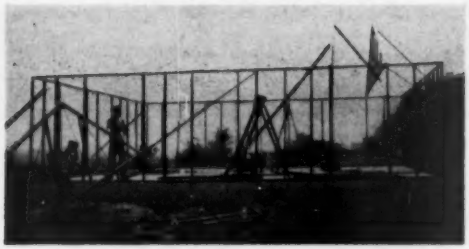

Fig. 4 Setting of roof trusses after outside wall framing was completed. Inside partitions framing do not carry load of roof

The steel shell (Fig. 3) of the house was completed in 6 days by 2 laborers and a welder. The laborers had never handled a piece of structural steel before. The frame, of 16 gage cold-rolled structural shapes, was completed in two days, as shown in Fig. 4. Four more days were required to fit and weld into place all the outside wall sheets (13 gage copper-bearing steel), window frames and front and back door frames. In the next two days, the roof was nailed on, the chimney assembled, the partitions and inside door-frames welded into place. The shell was then packed with insulation, rock lath applied ready for the plasterers and the plumbing and wiring roughed in. Both board and blanket insulation were used on the outside walls. The plastering, and the drying time necessary between coats, took up the largest amount of time in the building of the house. Dry-wall construction would materially shorten this time. The plasterer-contractor stated that in all of his years of experience, the walls were the straightest he had ever worked on and this resulted in a superior plastering job and a saving in materials, because there were no hollow areas to be filled and leveled. No caulking was required to weatherproof the doors or windows because of the continuous welded design. Steel door frames were used throughout. The door frames for the entire house were set by welding to the framing in about the time it would take a carpenter to cut and set the frame of one door of wood. This house is built without basement, on a concrete slab, insulated with 1-in. insulation board to prevent frost from entering. Two layers of heavy asphalt paper separate 8 in. of gravel from the $3^1/_2$-in. concrete slab. Asphalt tile was used to finish the floor. The outside walls were finished with four coats of white lead paint, and while the fourth coat was still wet, beach sand was applied; after the fourth coat plus sand dried; a fifth coat was applied, giving a very pleasing smooth stucco finish as well as an unusually durable one.

The welder followed a definite welding procedure and sequence so as to keep distortion to an absolute minimum. The success of the house was due to the fact that the outside walls did not distort or buckle due to the heat of welding.

Why build a house of steel? The steel framing and steel outside wall construction eliminates the risk of getting a jerry-built house in the low-cost field. You get a house properly engineered with studs and beams welded to have definite design strength. There is no green wood to dry out and shrink and draw nails loose. It does not warp, dry out or buckle; door or window frames do not slip out of square; the sills and studs are rot proof; the walls are true, square and straight.

You get greater economy in man-hours of labor: For instance, a 4 x 8-ft. outside wall sheet can be erected and welded in the time it takes a mason to lay 14 or 15 bricks. In ordinary construction, about 50% is spent for labor, 50% for materials; in site-welded steel construction, the labor is 30%, materials, 70%. The 20% savings on labor is applied to better quality materials.

You get economical maintenance: It is free of the threat of decay facing homes built of lumber; it eliminates the possibility of damage by termites; it is grounded for lightning and eliminates the necessity of fire insurance on the house itself. General upkeep is at a minimum because of rigid, shrinkproof construction.

This method of construction is completely flexible and can be used with or without basement, and adapted to any floor plan. I believe it would have greater resale value because deterioration is negligible.

Hi-Thoria Tungsten Reduces Cost

A NEW tungsten electrode which will reduce inert-arc welding costs has been announced by the General Electric Co.'s Welding Divisions, Fitchburg, Mass.

Major applications of the new tungsten will be for inert-arc welding with direct current, straight polarity, using either argon or helium gas. This type is usually used to weld mild steel, stainless steel, copper and *alloys*, G-E engineers said.

The new Hi-Thoria tungsten electrodes will produce a stable arc over a wider range of currents and will resist contamination by weld metal, according to the engineers, resulting in over ten times normal life. Hi-Thoria tungsten has strength comparable to standard pure tungsten.

The end of a standard tungsten electrode when subjected to arc temperatures tends to become molten and forms a round shiny surface. The molten surface sputters off into the work in relatively large quantities on arc starting and in minute quantities from time to time when welding, so that the tungsten is slowly consumed, G-E engineers explained.

Hi-Thoria tungsten, however, runs cooler than standard tungsten and does not become molten. The end remains square and intact and consumption is reduced. According to the engineers, it is seldom necessary to stop welding in order to clean or break off the end of the electrode. The arc tends to stay under the electrode and will not wander or climb up the electrode at low currents, assuring consistent ease of operation. Touch starting is easily accomplished with currents as low as 5 amp. The new electrode is manufactured in 3- to 24-in. lengths and from 0.040 to $^1/_4$ in. diameter.

Hi-Thoria tungsten is not recommended as a standard tungsten replacement for reverse polarity d.-c., a.-c. welding or atomic-hydrogen welding.

New Developments in Flame Cutting Stainless Steel

The problem of flame cutting stainless steels is mechanical and chemical, namely the removal of the refractory oxides which form by means of a suitable flux as not to block the cutting oxygen

by G. E. Bellew

THE trend to the use of more and more stainless steel, which became quite apparent at the end of the last war, has continued steadily, with a further trend showing up toward the use of higher alloy materials, up in the range of 30, 40 and even 50% total alloy content. With the steady increase in the use of such materials, the prospects of using flame cutting for stainless steels have become increasingly attractive, and particular interest has been felt in cutting the higher alloy analyses, and in cutting thick stainless risers in the foundry field.

The problem involved in cutting stainless steels is a mechanical one, similar to that involved in welding them. At high temperatures, in the presence of oxygen, the alloying elements (particularly the chromium) form refractory oxides, which do not melt and slag off as does the iron oxide in mild steel cutting. Instead, these high-melting-point oxides solidify, and stop the cut by blocking the cutting oxygen from further attack on the steel. By elimination of these oxides through use of a suitable flux, much as they may be removed in welding, the cut is enabled to continue, and the operation may be carried out in much the same way as in mild-steel work.

A process, using a powdered nonmetallic flux for the purpose, was introduced successfully some years ago, and to a considerable extent, solved the problem of flame cutting many stainless types and analyses. This process, known as Flux-Injection cutting, uses a finely divided nonmetallic flux of comparatively simple composition, which is carried directly in the cutting oxygen stream. With this method, the flux and the cutting oxygen reach the face of the cut simultaneously, and the refractory oxides are fluxed as fast as they form.

The process uses, with minor adaptations, standard cutting equipment. Three hose torches, standard design tips and other accessories were all retained, and the flux was fed by a variable-speed vibratory hopper, or Flux Feeder Unit, directly to the cutting-oxygen stream. This was made possible by passing the cutting oxygen through the outlet side of the Flux Feeder Unit on its way to the torch.

With the original process, it was possible to obtain cuts of very satisfactory quality, and at very reasonable cost, on many of the widely used analyses of stainless steels. Successful installations in a sizable number of fabrication shops realized substantial economies for their users, and as long as the work was done on straight 18–8 and lower alloy types of plate material in reasonable thicknesses, from $\frac{1}{4}$ to $2\frac{1}{2}$ in. good success was obtained. A typical job of this nature is illustrated in Figs. 1 and 2. In this case, the material is 18–8 stainless plate, $\frac{3}{8}$ in. thick, and the odd-shaped sections, blades for a papermaking machine, are cut in multiple from a single plate, with minimum scrap losses.

In spite of such successes, however, an important area of application for the process could not be covered with the original method. Risers of any important thickness, slabs and material of such analysis as 25% chromium, 20% nickel proved to be impractical for cutting with the existing factors of the process.

The primary need, obviously, was a better, more effective flux than the original compound. In obtaining such an improvement the research team was faced with certain major limitations. Some of the most effective fluxes for the purpose are ruled out for safety reasons. The flux must be inexpensive, which rules out other effective materials. It must be noncritical in its storage, handling and similar characteristics—and yet it must be an effective fluxing agent for the troublesome refractory oxides.

Working within these and other restrictions, the research team on the problem developed a fluxing agent which will do the job. It is a simple, relatively nontoxic* material, comparatively low in cost, and highly effective for the elimination of the oxides which interfere with cutting stainless. It does all the things the older flux will do, and further extends the application of the process, for it permits free cutting of sizable stainless risers, of heavy slabs and ingots, and of the 25% chro-

* Fumes from cutting may, however, be injurious, and adequate ventilation and protective masks should be provided during all operations.

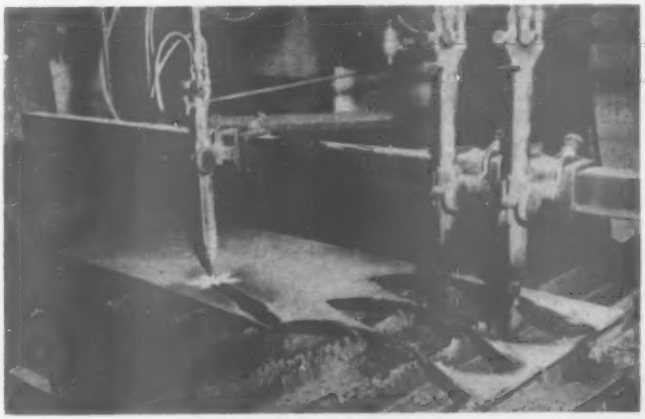

Fig. 1 Typical setup for flux injection cutting, using an oxygraph to cut blades for a papermaking machine

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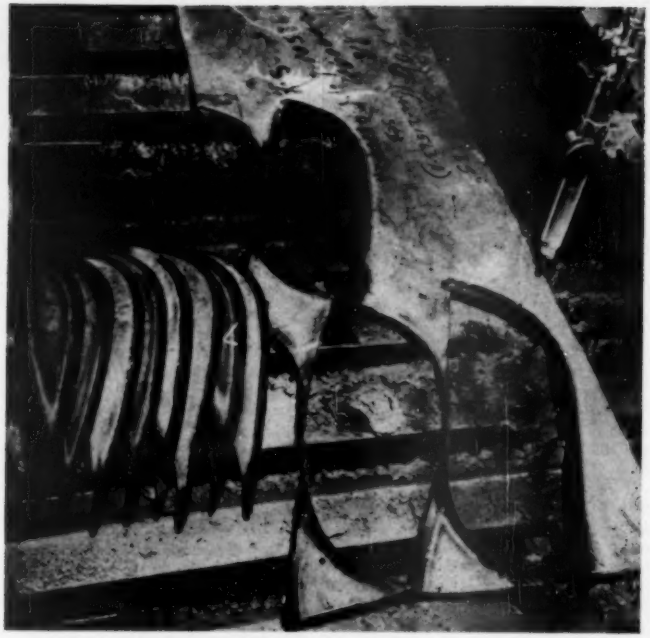

Fig. 2 18-8 Stainless steel blades, 3/8 in. thick are flux injection cut with minimum scrap losses and maximum efficiency

mium, 20% nickel; 25% chromium–12% nickel, and the 35% chromium–15% nickel high alloy analyses.

The mechanical nature of this flux is somewhat different from the original material. It is coarser, harder and more abrasive, and it requires some slight modification of the apparatus used with the process to resist this abrasive behavior. Abrasion resistant hose is required, from the Flux Feeder Unit to the torch, and in certain connections in the Flux Feeder Unit itself. It is also necessary to use a special tip, with an abrasion-resistant stainless steel insert for the cutting oxygen orifice. These tips, while of standard design, are specially made up for this process—a standard copper tip has a drastically shortened life with the flux flowing through it.

The only other equipment change of any importance is a recalibration of the variable flow control for the Flux Feeder, and all the changes required are easily and inexpensively made on existing equipment in the field. There are no fundamental alterations in the basic operating setup.

In adapting any standard steel-working process to apply it on the stainless alloys, it is almost always necessary to modify operating conditions and procedures, and this is equally true of Flux Injection cutting. Not only is a flux added to the factors involved, but slightly heavier preheat is used. Cutting oxygen pressures run a little higher than they do on mild steel—although with the new flux, they are closer to the mild steel figures than they were with the original process. Generally speaking, the Flux-Injection process requires the use of a slightly larger tip than would be used for the same thickness of mild steel, and otherwise, the general rules of good flame cutting practice govern the operation.

In actual operation of the equipment, only one additional step at start and finish of the cut is involved, once all pressures and speeds are adjusted—the switching on and off of the Flux Feeder Unit. A simple rule governs this operation—the unit must never be running unless cutting oxygen is flowing. Thus, the cutting oxygen is turned on before the feeder unit starts, and the feeder is cut off before the oxygen stream is stopped. Adherence to this simple formula effectively prevents the flux from clogging in the feed channel, hose or the torch body.

Properly adjusted and controlled operations, working at almost equivalent speeds, produce cut quality that is in almost every way comparable to mild steel work. Typical cuts exhibit a uniformly smooth and clean face; intersecting cuts meet sharp and clean at corners, and a high degree of cut control is available.

As far as cutting capacity is concerned, the upper limit of thickness which can be cut is greater than that of the usual commercial ingot, riser or slab, while the lower limit of efficient cutting—except, perhaps, through stack-cutting operations—is about 1/4 in. Below that thickness, most stainless fabrication shops are well equipped for mechanical cutting of the material, and the type of work required on much sheet material would not permit flame-cut edges.

Risers in stainless steel foundry work are a perennial problem, but with the new flux, they can be cleanly severed with the torch, in a very short time, leaving a minimum flat pad for grinding—and often obviating the grinding operation entirely. Compared to the previous most commonly used method—arc cutting—both quality and economy are very substantially improved, as the following tabulations of actual cuts on large risers indicate:

Table 1—Riser Cutting, 11-x 5-ft. Ring Riser to Charging Box Sizes. All Cuts on the Same Cross Section

Arc cuts		*Flux injection cuts*				
Cut	*Time, min.*	*Cut*	*Time, min.*	*Cutting O_2*	*Preheat O_2*	*Acet.*
1	21	1	12	45	15	8
2	22	2	11	45	15	8
3	21	3	10	45	15	8
		4	8	45	15	8
		5	9	45	15	8

Table 2—Round Riser Cutting, 1/2 In. Thick x 4 1/2 In. diameter. All Cuts on the Same Cross Sections

Arc Cuts		*Flux injection cuts*				
Cut	*Time, min:sec*	*Cut*	*Time, min:sec*	*Cutting O_2*	*Preheat O_2*	*Acet.*
1	2:40	1	0:40	40	15	8
2	2:45	2	0:46	40	15	8
		3	0:40	40	15	8
		4	1:10	40	15	8

That there is a very substantial time saving between the two methods is apparent from these figures. What the figures do not show, but which is also very important in foundry work, is the great difference in clean-up time and work involved between the ragged, hard-to-con-

trol are cut and the smooth, flat face of a Flux-Injection flame-cut pad. In this respect, it will be possible for the first time for stainless steel foundry cleaning rooms to obtain the cost and time saving advantages of machine gas cutting for riser removal that have been familiar for many years to their colleagues in plain steel casting operations.

A production type run on a complete job handled with the flux injection torch gives an even clearer picture of the operating economy and speed of the process. In this case, as sketched in Fig. 3, the job consisted of a ring casing with 8 risers 10 x 11 in. one riser 9 in thick, 14 in. long and a triangular pad to be trimmed after removal of each riser which was 6 x 10 in. in cross section. The material is Type 304 stainless. Risers were cut with 45 psi. cutting oxygen, 15 psi. preheat oxygen and 8 psi. of acetylene, in an average time of 9.37 min. Trimming cuts, using 35 psi. cutting oxygen and the same preheat settings, were completed in an average time of just over 7 min. Total cutting time for the entire job runs about two hours and a quarter.

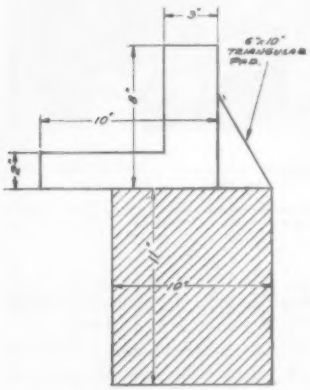

Fig. 3 Cross section of casting and riser, with riser section shaded

In another case, the job consists of 7 risers $1^1/_2$ x 4 in. long, 16 web cuts $^1/_2$ x $^3/_4$ in. and 2 scrap cuts 1 x 1 in. On this casting, riser cuts average 45 sec. each, and scrap and web cuts totaled 7 min. Cutting twelve of these rings, totaling 84 risers, 192 web cuts and 24 scrap cuts consumed only 6 hr., 45 min., total elapsed time, using 40 psi. cutting oxygen, 15 psi. preheat oxygen and 8 psi. acetylene.

One of the leading producers of stainless steels has found the Flux Injection process, with the new flux, a valuable facility in their rolling operations, working on material of even larger size and greater thickness than the foundry jobs. The problem of cropping, trimming and otherwise cleaning up ingots, billets and blooms in stainless production has always been important and troublesome. The material must be handled more carefully, quality standards are higher and most means of cutting it are more expensive, than for mild steel. When the original Flux Injection compound was tried for these purposes, it did not live up to expectations, but today, with the new Flux, the process is proving thoroughly successful, and has received prompt and complete acceptance.

One example of the problem the producer of stainless meets in the rolling mill is the preparation of rerolling blooms for further working in customer plants. These blooms often emerge from the breakdown rolling in the primary mill with their ends cocked, ragged or otherwise unsuitable for satisfactory further processing. In such cases, customer requirements make it all but mandatory to remove the defective material.

The section of the blooms in this particular case is 5 in. thick x $7^1/_2$ in. wide, and the material is Type 430 straight-chromium steel. With Flux Injection, using a No. 7, Style 164 tip, 50 psi. cutting oxygen, 15 psi. preheat oxygen and 8 psi. of acetylene, the cut is made through the 5 in. thickness in from 2 min. to 2 min., 10 sec.—a cutting speed of somewhat better than 3 in. a minute. The finished cuts, as illustrated in Fig. 4, are flat, smooth and of generally excellent quality. The kerf width is about $^1/_4$ in., and there are no nicks, gouges or rough spots on the cut surface.

One requirement of this particular job is that the cut must be not only of good appearance and clean contour, but that there must be no dilution or other metallurgical change in the steel. Since the flux used with this process has no effect on the base metal, but acts entirely on the oxides in the cut, and since whatever heat effects may arise will be eliminated in heating for rerolling, the process is completely satisfactory on this score.

An even more impressive job, done in the same mill, is the cutting of Type 302 stainless steel ingots which have been subjected to five or six breakdown passes before cropping the ends. These 10-ft. long pieces are 37 in. wide by 12 in. thick, and even in mild steel would qualify as a heavy cutting job.

Using Flux Injection, with a No. 8, Style 82 tip, and 100 psi. cutting oxygen, 20 psi. preheat oxygen and 14 psi acetylene, these sections were cut in $12^1/_2$ min. each—a cutting speed of almost exactly 3 in. per minute. This speed is increased appreciably with a larger size tip and somewhat lower cutting oxygen pressure. The quality of the cut as illustrated in Fig. 5, is excellent.

This same mill, in its maintenance shop, uses the process for a variety of plate parts for repairs and construction, and since they use considerable scrap for this purpose, the analysis of the material is not always certain. This lack of certainty has, so far, caused no difficulty, as the Flux Injection torch has been able to cut whatever material, in whatever thickness, was put on the worktable.

It seems apparent that the development of the new flux for the process has made

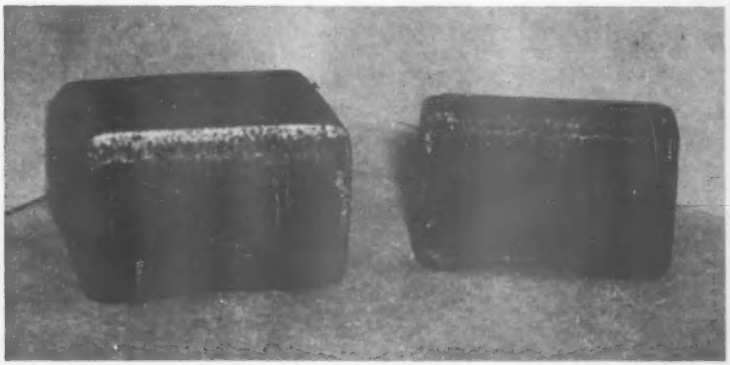

Fig. 4 Cut faces of 5- by $7^1/_2$-in. type 430 stainless steel rerolling bloom

Fig. 5 Cut face (left) of a 12- by 37-in. ingot of type 302 stainless steel

Flux Injection cutting fully capable of doing all the things for which it was originally developed. With no apparent limits, as we stand today, on material analysis or thickness the process is cutting stainless steels very nearly as readily, and with as good quality, as we have been accustomed to cut mild steels.

There is reason to believe, also, that the process may solve at least one other problem of a "cut-proof" ferrous material, for there has been some successful experimentation on cast iron, and with further testing, it is expected to prove capable of production cutting work on that material also. As is often the case when a process is subjected to further study seeking improvements in its original conception, Flux Injection cutting has not only been made a more satisfactory and competent operation, but its scope appears to have been widened even beyond the original intentions.

Automatic Welding on Propane Tanks

by C. G. Herbruck

GOOD fixturing is speeding the automatic hidden arc welding of propane cylinders at Creamer and Dunlap in Tulsa, Okla.

Cylinder heads are fabricated in the powder-driven fixture shown in Fig. 1. The head crown is placed first in the fixture and a cap plate clamped on it by an air cylinder. The weld is made, the piston raised and the boss placed on the cap plate. As the boss is welded the operator cleans the first weld with an air chisel.

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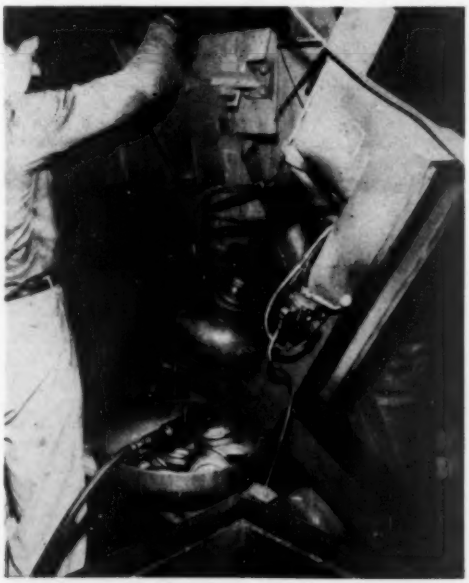


Fig. 1 Cap plates and bosses are welded on 100 lb. propane tank cylinder heads in this welding fixture

Weld on cap plate is being made first. Bosses, in container in foreground, are clamped on and welded after cap plate is welded

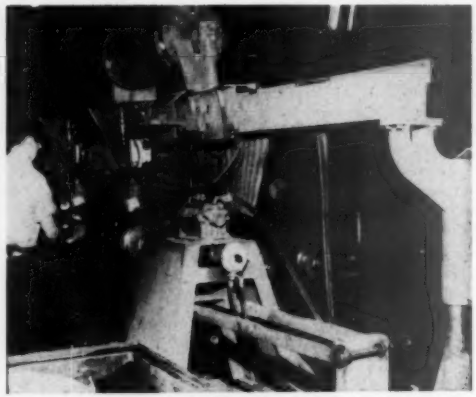


Fig. 2 Two "Lincolnweld" automatic units weld bottom and head to cylinder shell simultaneously

Tanks come into fixture from rear and roll out the front. Fixture is adjustable for several sizes of tanks. Production is 25 per hour

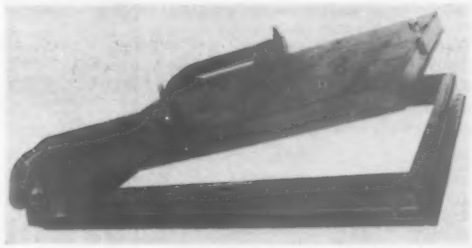
The fixture is inclined about 20° to facilitate electrode positioning and permit flux to slide into a receptacle to the right. Hidden arc welding is done with a "Lincolnweld" automatic head using a $^1/_8$-in. diameter electrode wire at 400 amp. and 28 v. The fixture turns out approximately 30 heads per hour.

The head and bottom are swedged into 10-gage cylinder shells and the assembly is moved to another automatic welding fixture for welding the two lap joints simultaneously. The cylinder assembly rolls down an inclined gravity conveyor into the welding fixture from the rear. It is picked up by a scissor-action elevating device. Air-actuated arms clamp the head and bottom into the shell when it has been elevated to the welding position and the elevating mechanism is dropped. The clamping arms turn the cylinder for welding.

Two automatic heads weld the head and bottom simultaneously, using $^1/_8$-in. wire at 425 amp. and 31 v. Approximately 25 tanks per hour are completed in this fixture.

This double-head fixture is flexible in that it can be adjusted for different size tanks. The welding heads can be positioned on a beam which is either raised or lowered. The clamping arms and elevating device can also be located to accommodate several sizes.

Tide Gates



Welded tide gate

IT IS extremely important that all wastes from municipal sewage systems shall be discharged as rapidly as possible into the treatment plants. During periods of storm flows, the accumulation of this diluted sewage becomes too great for disposal plant, and it becomes necessary to by-pass the flow to adjacent streams or into tide water and at the same time divert to the treatment plant as much flow as may be handled to advantage.

The outfall sewers are usually submerged, and suitable tide gates must be installed in conjunction with a dike to permit the storm-water overflow but also to prevent the backflow from the tidal basins.

It is necessary that these tide gates shall start to open with minimum loss of head; otherwise, the storm flow will tend to back up into the sewers system, perhaps flooding into the streets. The City of New York, Department of Public Works, has developed, together with Krajewski-Pesant Mfg. Corp of New York, a new type of pontoon tide gate, as shown by the accompanying photograph, which has sufficient buoyancy to permit opening under a 2-in. differential head. The wall casting and frame are made from high-test cast iron, while the flaps are constructed entirely from genuine Byers wrought-iron plates and membranes making a multiple number of watertight compartments continuously welded inside and outside from $^{3}/_{8}$-in. thick plates. These have facing strips finely machined which seal against $1^{1}/_{2}$-in. square rubber seating strips in dovetailed grooves. The gates, being in extremely corrosive waters, many times brackish and salt, require maximum protection. The use of genuine wrought iron gives the maximum of protection against deterioration. The manufacturer further coats these with Koppers Company B-70 coating with the hot-enamel application.

Ideas for Designing Machinery Parts

Thought starters on welded design furnished through the courtesy of the Lincoln Electric Co., Cleveland, Ohio

ARC welding allows the designer to initiate many cost-saving ideas in the manufacture of machinery parts. Production operations can be generally simplified over bolting and riveting . . . the need for patterns and castings eliminated and service difficulties arising from breakage or looseness avoided. The following examples illustrate how one machinery builder has redesigned his equipment one part at a time to improve product strength while using less material for manufacture.

WHEELS

Original construction called for a gray iron hub welded to a steel rim (Fig. 1).

Fig. 1 Welding the assembly of component parts to fabricate stronger, more rugged wheels

To avoid breakage from unexpected shock steel design has now been adopted using simple tubing for the hub and a disk blanked and formed from steel plate. Steel components are clamped in a fixture and welded, cutting the cost 62% while reducing weight 69%.

FORKS

Former riveted design was found to work loose and eventually fail in service. Single piece construction made possible by welding the spline shaft to the steel fork has increased strength 70% and has eliminated breakdowns and service problems.

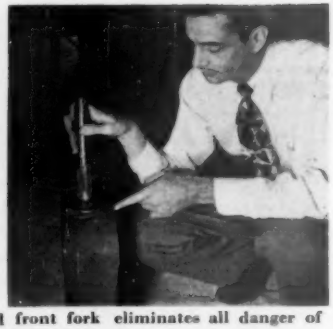


Fig. 2 Unit construction of all-welded front fork eliminates all danger of looseness

LEVERS

Through the flexibility of arc welding, machinery levers can be more easily fabricated to withstand heavier loads while keeping down weight factors. Since steel is three times stronger and $2^1/_2$ times stiffer than cast iron, stronger yet lighter members can be weld-designed to insure more efficient operation with less dead load, less vibration.

The lever illustrated in Fig. 3 shows how standard steel shapes can be utilized. The hub is square tubing welded to a strap formed from steel bar. The yoke, welded to the end of the bracket, is formed from steel.

FRAMES

Where shock and impact are likely, all welded frame construction (Fig. 4) is found to be preferable to riveted design. The solid, unit construction of welded design eliminates the common occurring difficulties, where once a single rivet loosens, complete failure is often the eventuality. Frame members are weld-designed for simple assembly in clamp-type fixtures that can be swung or swiveled for easy downhand welding on all joints.

Welded design allows greater freedom in simplifying production procedures. Machining of component parts can be done on smaller, faster machines prior to welded assembly, thus eliminating the need for costly and slower equipment. Figure 5 shows a formed steel bracket in which the holes in the top and side had to be drilled after forming to maintain correct center to center distances. By redesigning for arc welding, the bracket is now made in two pieces, each pre-drilled in the flat position. The two components are then clamped in a fixture, locating from the holes and welded with Automatic hidden arc process. Savings made in the first six months of production of these brackets have entirely paid the cost of the fixtures and welding equipment required.

Fig. 3 Weld-designed arm avoids breakdown from shocks and impact

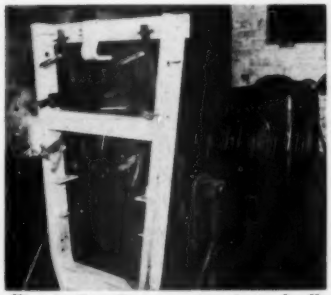


Fig. 4 Sturdier construction of all-welded frame resists unusually severe operating loads

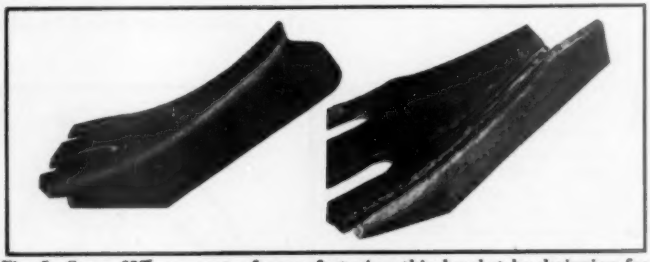


Fig. 5 Saves 33% on cost of manufacturing this bracket by designing for welded fabrication

Removing Discoloration from Stainless Steel

A SIMPLE and economical method for removing weld discoloration from stainless steel has been developed by the Research Division of Armco Steel Corp. It is especially useful in removing discoloration from hard-to-get-at interior corners.

Welding and sheet metal shops will find the device easy to make and handy to use. It was designed to meet the need, recognized by every shop man who works with stainless steel, for a simple low-cost method of cleaning interior welded seams and corners.

Armco's new method of removing weld discoloration is easy both to make and to use. A small amount of acid solution to cover the discolored area, a copper rod, a d.-c. power source and electrical connections are its only parts. And the cleaning not only removes weld discoloration but passivates the cleaned area.

The essential parts and assembly of the equipment are shown in the photograph. A copper rod about $^1/_4$ in. diameter is bent to a convenient shape. Short pieces of rubber tubing are placed on the rod to keep it from touching the stainless steel and shorting the electrical circuit. Only enough 50% phosphoric acid is poured into the welded corner to contact the copper rod and wet the discolored weld area.

The copper rod is connected to the negative terminal of the d.-c. power source and the stainless part to the positive terminal. Then the copper rod is passed along the joint to be cleaned.

Since the cleaning action depends on the electrical current passing through the solution, the copper rod must not touch the stainless. Weld discoloration is removed quickly; the rod can be moved at a rate of about 2 ft. per minute.

Other acids can be used but will etch the surface more than phosphoric. Likewise the acid concentration controls the appearance of the cleaned surface—higher concentration gives a brighter finish. The power source should provide about 6 to 9 v. direct current and 5 to 6 amp. are required for each square inch of rod in contact with the acid. Thus, a $^1/_4$-in. diameter rod with a cleaning section 2 in. long and half submerged in acid would require about 4 to 5 amp. Since high-current densities give better cleaning, the acid solution should not be deeper than recommended or cover too wide an area. Better results are obtained by cleaning small areas, using more than one pass of the rod if necessary.

A 6-v. storage battery is satisfactory for small jobs but a 12 v., 50 amp. rectifier is desirable for higher production or larger work. Low capacity d.-c. welders also can be used as a power source for cleaning large welded structures. It may be necessary, however, to use resistors in the circuit to reduce the voltage.

Where weld discoloration covers a large area, a modification of the cleaning method is very effective.

Instead of a copper rod, a flat copper strip wrapped with acid-dampened asbestos or glass cloth is used. The strip is passed over the area to be cleaned. This method requires higher currents because of the larger contact area but the same current density, 5 to 6 amp. per square inch of copper strip.

This novel cleaning method will remove light scale and discoloration but will not remove heavy scale or weld slag. Heavy deposits should be removed by chipping or brushing before the cleaning tool is used.

Both large and small shops using the tool have found that it not only does a good job of cleaning stainless welds, but cuts finishing costs.

Fig. 1 Equipment for the cleaning method is extremely simple

Only a battery or rectifier for d.-c. power, acid solution, wire, clips and a copper tool are necessary. The copper rod is used for corners and the copper strip for cleaning flat areas. The right half of the sample has been cleaned with the equipment shown

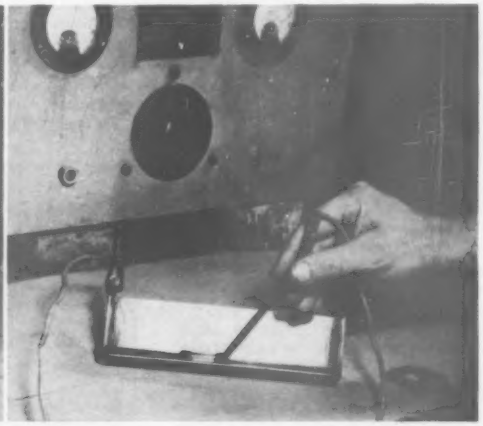


Fig. 2 Cleaning discoloration from stainless weldments is just as easy as the photograph shows

The part is connected to the power source; acid solution is applied to the weld area and the copper tool passed along the weld

Steel Warehouse Building Flame Cleaned Without Necessity for Work Shutdown

FLAME cleaning is proving a very effective method of giving old structural steel the "new look" with a minimum of time and cost. The flame cleaning and reconditioning of one storage building, typical of similar buildings in almost every town and city in the country, was an application of this process to a problem that could have been solved in no other way without seriously inconveniencing the owners. The structural steel was in rather bad shape and thorough reconditioning was required. However, it was essential that regular operations be carried on while the cleaning was in progress.

Due to the health hazard to the workers, sandblasting was impossible and wire brushing was ruled out as being too costly because of the very heavy accumulation of dirt, oil and rust. After consultation, it was decided that the work could be done best by flame cleaning.

This job was a good demonstration of the flexibility of the flame-cleaning process. The structural members to be cleaned varied in size and shape from 16-in. I-beams to 2-in. angles. Stored materials, which extended in some cases from the floor to the ceiling, would have been costly and awkward to move or cover for sandblasting. The work had to be done from floor level to 40 ft. above ground.

Notwithstanding these handicaps, the actual cleaning and reconditioning work proved easy. The labor force consisted of two men. One was a flame cleaner who operated a standard cleaning torch with extension, and then followed up the work he had done with a wire brush. The second man swept up the residue and then applied a coat of protective paint to the cleaned area while the steel was still warm.

Figure 1 shows a riveted, gusseted roof truss joint before flame cleaning. Note the scale, flaked paint and pitting that had accumulated due to several years of neglect.

In Fig. 2 the operator is flame cleaning the joint shown in Fig. 1, using a round, multiflame tip, Style No. 110, for surfaces where there were angles, corners and rivet heads.

Figure 1

The same joint, after flame cleaning and wire brushing, but before painting, is pictured in Fig. 3. Note thorough job that has been done in removing the rust, scale and dirt. Figure 4 is an illustration of a similar joint after flame cleaning, wire brushing and painting.

On this job one of the advantages of flame cleaning over other methods was the lightness and flexibility of the equipment, making it unnecessary to erect scaffolding to reach out-of-the-way positions. The operator simply climbed over the material or, where there were no cross-members to stand on, a ladder provided adequate support. Another advantage lay in the fact that at no time was there any interruption of the normal warehouse operating schedule.

The quality of the work done means that the work will not have to be repeated for a long time. The reason for this is that the flame-cleaning method provided the best surface for the application of the paint. The painter followed closely behind the cleaner and applied the paint while the steel was still warm. The heat drove all moisture from the surface so that the paint adhered more closely and will therefore last much longer.

In general, it may be said that flame cleaning does a much more thorough and durable job on structural steel surfaces than any other method of reconditioning. The process has been applied successfully to bridges, ships' hulls, storage tanks, interior and exterior structural steel, steel towers, craneways, prefabricated steel members and many other jobs.

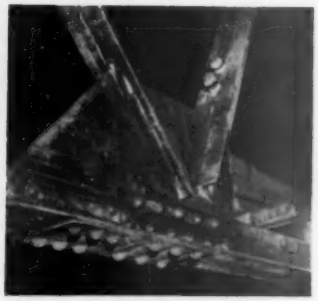


Figure 3

Figure 2

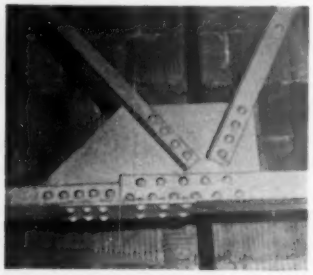


Figure 4

Data & Photos Courtesy Air Reduction Sales Co.

the AMERICAN WELDING SOCIETY

activities • related events

Meeting Board of Directors

A Board of Directors meeting of the AMERICAN WELDING SOCIETY was held in the Hotel Statler, New York, N. Y., on Thursday, Dec. 14, 1950, at 10:00 A.M., with the following in attendance:

Board Members: H. W. Pierce, *Chairman*, C. H. Jennings, F. L. Plummer, R. S. Donald, L. C. Bibber, J. J. Chyle, T. J. Crawford, O. B. J. Fraser, A. J. Moses, H. E. Rockefeller, J. R. Stitt. *Staff:* J. G. Magrath, *Secretary*.

Guests: C. D. Evans, Chairman, Technical Activities Committee, and J. Landau, David Joseph Co., A.W.S. Auditors.

Absentee members were: C. A. Adams, A. G. Bissell, W. F. Boyle, A. F. Davis, J. Grodrian, L. Grover, H. O. Hill, J. H. Humberstone, T. B. Jefferson, G. S. Schaller, G. N. Sieger, H. N. Simms, L. C. Stiles, H. S. Swan, C. B. Voldrich, A. E. Wisler.

Honorary Membership: Inscription for Honorary Membership Certificate for Mr. R. T. Gillette, as submitted by the Committee consisting of Dr. W. F. Hess and Mr. R. W. Clark, was read and with some additions approved.

Metallizing Committee Fund Raising

The A.W.S. Technical Committee on Metallizing, through the Technical Activities Committee, requested permission to solicit the sum of $5000 for a program of testing metallizing applications for corrosion resistance.

It was voted that the A.W.S. Technical Committee on Metallizing be authorized, in the name of the AMERICAN WELDING SOCIETY and within the limitations of the A.W.S. By-Laws, to solicit the manufacturers of metallizing equipment and metallizing contractors, for a sum of $5000, within a limited time of six months, for a program of testing metallizing applications for corrosion resistance, the funds to be turned over to the SOCIETY'S Treasurer and to be earmarked for such purpose.

Fund-Raising Committee

The Executive Committee, on Sept. 21, 1950 (action approved by Board of Directors on Oct. 26, 1950), recommended that the Finance and the Technical Activities Committees each appoint representatives to a joint special committee with scope to devise a program designed to secure funds from industry for conducting more extensive technical activities.

It was voted to (*a*) not enlarge the Committee, and (*b*), to accept the Finance and Technical Activities Committees' recommendations of appointees, namely, Messrs. R. S. Donald and O. B. J. Fraser for the Finance Committee, and Messrs. C. D. Evans and R. W. Clark, with J. Lyell Wilson as alternate to C. D. Evans, for the Technical Activities Committee.

1950–51 Budget Adjustment

The Secretary presented a request of the *Welding Journal* Editor that approval of the Board be given to increasing the 1950–51 Budget in the amount of $5000 on both Income and Expense sides, to accommodate recently announced cost increases of *Journal* printing (expense) and anticipated increased revenue from the raised advertising space rates (income).

It was voted to approve the foregoing.

Membership Billing

Reopening was requested of the A.W.S. Detroit Section Officers' proposal that the present procedure of universal September 1st billing, with pro-rata billing to those joining the SOCIETY in the other months of the year, be changed to a "date of admission" or anniversary pattern.

It was voted that the plan for individual membership procedure be accepted in principle but that no change be made until the plan has been submitted to each Section management, through the Section Secretaries, and the majority replying have approved.

(Secretary's Note: If approved, the By-Laws require submission of the proposed change to the membership for vote, prior to adoption.)

. . .

At 3:50 P.M. Chairman H. W. Pierce, being required to attend a meeting in another city, vacated the Chair, calling upon Mr. C. H. Jennings, *First Vice-President*, to preside for balance of meeting.

A.I.E.E. Detroit Welding Conference

The American Institute of Electrical Engineers will hold a "Third" welding conference in Detroit in the spring of 1952. Our Detroit Section cooperated in joint sponsorship in 1950.

It was voted that the SOCIETY'S Secretary, a representative of the National Program Committee, and a Section Officer or Member designated by the A.W.S. Detroit Section should serve as a delegation of three to attend the A.I.E.E. Committee on Electric Welding organization meeting on Jan. 24, 1951, in New York, to explore the possibility of A.W.S participating in the Detroit A.I.E.E. Welding Conference on a National Society basis in the spring of 1952.

Informational Only

Annual Meeting Income

The Secretary reports that $6000 was received from the American Society for Metals for A.W.S. sponsorship and participation in the 1950 National Metal Exposition, and that $1770 was collected in Registration Fees. Net total Income for the week of Annual Meeting operations was $8043.31. Net total Expense for the week was $6039.16. Net excess Income over Expense was $2004.15. Salaries of staff members and other overhead items are not included in the above expenses.

Harnischfeger Donation

The Secretary reports that, through Mr. W. Harnischfeger, President, The Harnischfeger Corp. of Milwaukee, Wis., have, for the second year, contributed Fifty Dollars ($50) as an annual donation to our SOCIETY'S activities.

Appreciation to Past President

As ordered by the 1950–51 Board of Directors, at its Oct. 26, 1950, meeting, a letter of appreciation for his services to the SOCIETY as President was prepared by Mr. G. N. Sieger, *Member*, and Mr. J. G. Magrath, *Secretary*, and forwarded to Mr. O. B. J. Fraser, *President*, 1949–50.

Annual Meeting

The Annual Meeting of the AMERICAN WELDING SOCIETY will be held in Detroit during the week of the National Metal Congress and Exposition, October 14–20, 1951.

The headquarters of the AMERICAN WELDING SOCIETY will be at the Book-Cadillac Hotel. Several meetings of the National Program Committee have been held to select the best papers from more than a hundred offered for the occasion.

Special features are being arranged including an inspection trip, Annual Dinner and President's Reception.

Twenty-two technical sessions covering every phase of welding. Educational Lecture Series and other items will assure broad interest in the annual meeting program.

Copy of Diploma Tenderd R. T. Gillette

The AMERICAN WELDING SOCIETY—Organized 1919—By this Diploma certifies that the Board of Directors at a meeting held October twenty-sixth, nineteen hundred and fifty, elected ROBERT T. GILLETTE an Honorary Member by the adoption of the following Resolution:

WHEREAS, the application of resistance welding to the manufacture of electrical equipment has been due to a large extent to the sound judgment and developmental skill of Robert T. Gillette, whose wise guidance both of production engineers and of welding machine manufacturers, and whose insistence on high standards of quality have led to the successful and economic use of resistance welding;

WHEREAS, Robert T. Gillette has given direct service to the SOCIETY and long service on its various technical committees; and

WHEREAS, the By-Laws of the AMERICAN WELDING SOCIETY provide that by the unanimous vote of the members of the Board of Directors, Honorary Members may be chosen from among those who have rendered acknowledged eminent service to welding engineering or its allied sciences; it is

Resolved, that Robert T. Gillette be elected to Honorary Membership in the AMERICAN WELDING SOCIETY.

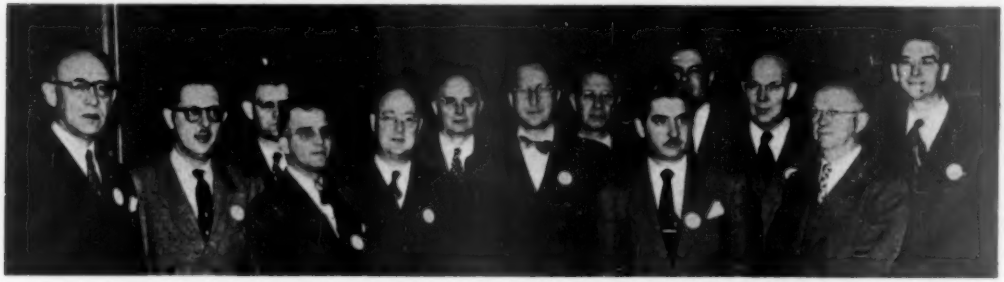
Witness our hand and seal at Chicago this twenty-sixth day of October 1950.

(s) O. B. J. FRASER, *President*
(s) J. G. MAGRATH, *Secretary*

A.W.S. Metallizing Committee Visits St. Louis

The Nooter Corp. of St. Louis, Mo., was host to the A.W.S. Metallizing Committee during their recent visit to St. Louis. Following its regular meeting at the De Soto Hotel, the Committee's members inspected the metallizing and welding operations at the Nooter Corp. Plant. The accompanying photo shows some of the members of the Metallizing Committee in front of the blasting room. The trip was most interesting and was enjoyed by all.

This committee has just completed recommended practices for metallizing. It is also considering a program of research dealing with the corrosion resistance of metal sprayed surfaces.



Members AWS Metallizing Committee Visit to St. Louis

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3 teeth partially rebuilt with AMSCO Repointer Bars to show sectional view of welding method. These Repointer, or Wedge Bars, are also made in many shapes and sizes. Apply AMSCO Economy Hardface to the rebuilt tooth *—for as much as 3 times longer service!*

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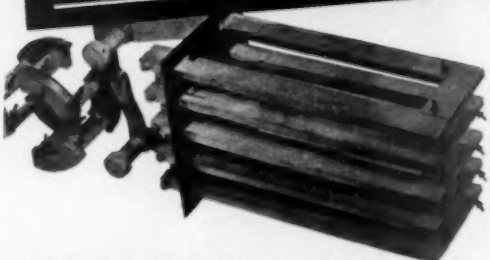

EUTECROD 1800 DOES "IMPOSSIBLE" JOB WITH EASE. In the magnet charger shown above, 23 copper joints had to be welded *without distortion*, for the cumulative effect of even the slightest warpage per joint would ruin the job. This seemingly impossible metal-joining problem was easily solved with EutecRod 1800—the fast-flowing, high conductivity, high strength "Eutectic Low Temperature Welding Alloy" that welds steel, copper, and nickel units that demand distortion-free elasticity. *(Write for complete data.)*

EUTECROD 16 STRENGTHENS RESCUE LADDER CONSTRUCTION. The Curtiss-Wright "Seahawk" collapsible ladder shown in the photo is constructed in cross-sections that swing together on hinges. Previous attempts to weld the ladder joints with conventional high-heat alloys resulted in misaligned structure that rendered the ladder unsafe. This unusual metal-joining problem was solved by using new EutecRod 16 and slight design modification. *(Details on request.)* The parts thus joined remained distortion-free and strong, surpassing the rigid Navy equipment requirements.

"EUTECTIC Low Temperature WELDING ALLOYS" are setting new standards for production and maintenance welding everywhere. Drawbacks of conventional brazing and fusion welding—such as distortion, warping, porosities, and weld failures—are minimized or completely eliminated when you use EUTECTIC's JOB PROVED hi-tensile low melting alloys.

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Independent Pioneer of the Welding Industry

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WELDING
CODES STANDARDS and SPECIFICATIONS

Are you fully informed on the latest welding standards available? Have you missed any of the earlier standards? This column is published as a regular monthly feature of The Welding Journal to keep you abreast of A.W.S. technical standards, which are universally recognized as the most authoritative source of welding information.

The list of publications shown below is only partial; it is changed from month to month.

Keep informed—Read this column regularly

A. FUNDAMENTALS OF WELDING

Definitions, Symbols, Testing, Filler Metal

A2.0-47 Standard Welding Symbols	$1.00
A2.1-47 Symbols Wall Chart	50c
A3.0-49 Standard Welding Terms and Their Definitions	$1.00
A3.1-49 Master Chart of Welding Processes and Process Charts	50c
A3.0-49 and A3.1-49 together	$1.50
A4.0-42 Standard Methods for Mechanical Testing of Welds (with 1945 Supplement)	50c
*A5.1-48T Specifications for Mild Steel Arc-Welding Electrodes (Tentative)	35c
*A5.2-46T Specifications for Iron and Steel Gas-Welding Rods (Tentative)	25c
*A5.4-48T Specifications for Corrosion-Resisting Chromium and Chromium-Nickel Steel Welding Electrodes (Tentative)	25c
*A5.5-48T Specifications for Low-Alloy Steel Arc Welding Electrodes (Tentative)	35c
*A5.6-48T Specifications for Copper and Copper-Alloy Metal Arc Welding Electrodes (Tentative)	25c
A6.0-40 Recommended Procedure to be Followed in Preparing for Welding or Cutting Certain Types of Containers Which Have Held Combustibles	50c

NOTE: 25% discount to A and B members and 15% discount to C members of AWS on copies of any codes and standards listed above except starred items.

Send your orders, or requests for Order Forms containing complete list of AWS publications to:

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FREE Eutectic Welding Alloys DATA FILE PROVES SAVINGS YOU CAN ENJOY IN MAINTENANCE WELDING

SALVAGE WELDING TIME CUT MORE THAN 90% WITH NEW Eutectic Welding Alloys

When a leading research institute's tensile testing machine was overloaded, the top snapped completely off the two side members. At first, the old-fashioned conventional high heat fusion methods were tried. The entire operation—pre-heat, slow cool, etc.—consumed 48 hours in all; and, on the first day after its repair, the casting cracked wide open again, through and alongside the welding area. It seemed as though the entire casting would have to be scrapped. Before taking this costly step, Eutectic was consulted . . .

The damage was repaired permanently with EUTECTIC's BIG TEAM —CutTrode + EutecTrode 24, for cast iron. CutTrode—a new special metallic-arc oxygen-less cutting electrode first beveled the breaks, in preparation for welding. Sufficient residual heat remained from the use of CutTrode to weld the entire job without any further pre-heat. The breaks were first tack welded; then stringer beads were deposited with new EutecTrode 24. The entire job — permanently welded — was accomplished in only 4 hours—1/12 THE ORDINARY WELDING TIME! (Complete details and photos of this job available on request.)

"EUTECTIC Low Temperature WELDING ALLOYS" are setting new standards for production and maintenance welding everywhere. Drawbacks of conventional brazing and fusion welding—such as distortion, warping, porosities, and weld failures—are minimized or completely eliminated when you use EUTECTIC's JOB PROVED hi-tensile low melting alloys.

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PERSONNEL

Walsh Made General Sales Manager

E. J. Brady, President of Alloy Rods Co., York, Pa., has just announced the appointment of E. R. Walsh III as General Sales Manager. Mr. Walsh has been associated with the company for several years as District Sales Manager and is well acquainted with the service problems continually met in working with distributors and dealers all over the country.

Mr. Walsh will devote most of his time to the distribution and sale of the complete lines of arc-welding electrodes for stainless steel, low-hydrogen, low-alloy and mild steels, cast and malleable iron. Alloy Rods Co. also manufactures a complete line of hard-facing and tool-steel electrodes.

Alloy Rods Co. Elects New Officers

At a recent meeting of the Board of Directors of the Alloy Rods Co., York, Pa., two new executive offices were created; C. R. Carlin was elected Vice-President in charge of Production and R. K. Lee was elected Vice-President in charge of Research and Engineering. Other officers elected at the meeting were E. J. Brady, President and Chairman of the Board; M. G. Sedam, Executive Vice-President; W. D. Himes, Treasurer; and H. L. Weaver, Secretary and Assistant Treasurer.

Mr. Carlin, newly elected Vice-President in charge of Production, has been with Alloy Rods Co. for a number of years as Sales Manager. R. K. Lee, Vice-President in charge of Research and Engineering, has been Manager of the Research Department for a number of years. Both are members of the AMERICAN WELDING SOCIETY.

Budd Appoints Thompson By-Products Sales Manager

Douglas E. Thompson has been appointed Manager of By-Products Sales by The Budd Co., Raymond F. Littley, Vice-President in Charge of Automotive Sales, has announced.

Mr. Thompson started with Budd at the Charlevoix Plant in Detroit in 1933, as a helper in the Salvage and Service Dept. He rose through various positions in this department and most recently was Superintendent of Salvage and Scrap for the Charlevoix Plant.

He will make his headquarters at Budd's Hunting Park Plant, Philadelphia. Mr. Thompson is a member of the AMERICAN WELDING SOCIETY.

Gerstacker Made Chief Engineer

William G. Gerstacker has been named chief engineer of The Colonial Iron Wks. Co., 17643 St. Clair Ave., Cleveland 10, Ohio. Colonial specializes in the fabrication of special processing equipment used in the chemical and allied industries.

Gerstacker has been associated with The V. D. Anderson Co., of Cleveland, as manager of the Solvent Extraction Division.

Born in Cleveland in 1913, Mr. Gerstacker received a Bachelor of Science degree in Mechanical Engineering from Case Institute of Technology in 1935, and a professional M.E. degree from the same institution in 1943.

Mr. Gerstacker brings into his new position his more than 15 years of experience in metal fabrication industries, having been an engineering estimator with The Pfaudler Co. of Rochester, N. Y., and a development mechanical engineer with Union Metal Mfg. Co. of Canton, Ohio,

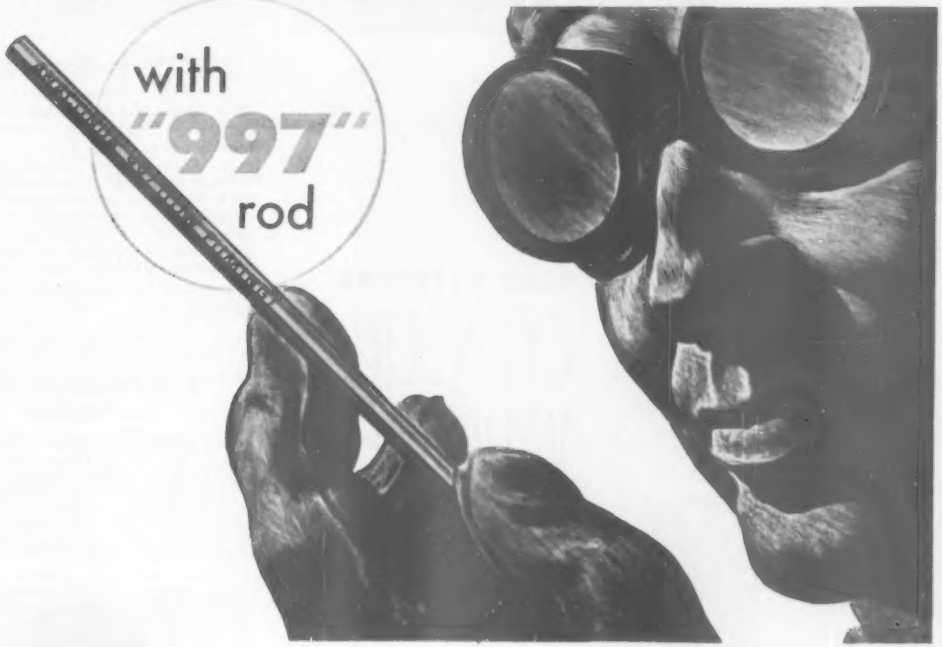
before joining V. D. Anderson six years ago. For four years during World War II he was night school instructor in strength of materials, machine design and engineering physics for the Engineering, Science, Management War Training program of the U. S. Office of Education.

He is a Registered Professional Engineer, a member of the AMERICAN WELDING SOCIETY and the Ohio Society of Professional Engineers. In 1942 Gerstacker was an award winner in the James F. Lincoln Arc Welding Foundation Award Program.

A. F. Davis Wins Award

Alton F. Davis, Vice-President and Advertising Director of Lincoln Electric Co., Cleveland, Ohio, has been named industrial advertising's man-of-the-year for 1950 in an annual competition conducted by the magazine, *Industrial Marketing*.

7" lap seam braze-welded in 25 seconds



THAT'S FAST TIME for braze-welding an automobile rear-quarter panel. And it's maintained.

Another large manufacturer in the same line says braze-welding with ANACONDA "997" Welding Rod eliminated five assembly operations in his production schedule.

ANACONDA "997" Rod is low fuming, has a low melting point (under 1600° F.), works fast.

For more information on this and other ANACONDA Welding Rods for better, money-saving, time-saving welding, write for Publication B-13. ANACONDA Welding Rods are available from distributors throughout the United States. The American Brass Company, Waterbury 20, Connecticut. In Canada: Anaconda American Brass, Ltd., New Toronto, Ont.

you can depend on

ANACONDA®

BRONZE WELDING RODS

REGO

HIGH PRESSURE

CYLINDER VALVES

for OXYGEN and HYDROGEN

Designed, tested and proved for high pressures and severe service.

Packing may be replaced with cylinder under pressure if necessary.

Fuse plug and bursting disc provide positive safeguard.

Bronze stem furnished as standard, also available with monel stem on special order.

for ACETYLENE

Cadmium-plated steel stem with monel tip resists rust and wear.

Ball-nose seat construction assures positive shut-off.

for CARBON DIOXIDE

Available with diaphragm-type or packed-type construction.

Compact rugged design for use with high pressures.

Equipped with bursting disc safety device.

*Reg. U. S. Pat. Off.

The BASTIAN-BLESSING Co.

4201 West Peterson Ave. Chicago 30, Illinois

PIONEER AND LEADER IN THE DESIGN AND MANUFACTURE OF PRECISION EQUIPMENT FOR USING AND CONTROLLING HIGH PRESSURE GASES

The competition, conducted since 1940 by the publication's "Copy Chasers," nationally known for constructive and occasionally caustic criticism of industrial advertising, is for the purpose of recognizing outstanding advertising published by industry.

Lincoln Electric's 1950 campaign, using editorial-type copy, promoted the company's welding process and equipment. More than 30 business and technical publications carried the award-winning advertising.

Mr. Davis, who has been with the arc-welding equipment manufacturer since 1914, is famous for his classic remark, "When I was in school I knew I wasn't the brightest boy in the class and decided then it would be necessary for me to work just a little bit harder. This I did, and I have managed to come under the wire with the gang."

W. R. Persons Elected Vice-President

At a meeting of the Board of Directors, Jan. 8, 1951, W. R. Persons was elected Vice-President-in-charge-of-Sales of The Lincoln Electric Co. He advances to his new position as an officer of the company from his present one of General Sales Manager which he has filled since 1946.

Mr. Persons joined Lincoln in 1934, going to the Pittsburgh branch office as a welding engineer. He graduated from the Case Institute of Technology with an M.S. degree in engineering in 1932. He was made District Manager of the Pittsburgh territory in 1937 and was active there until 1945 in welding development in steel-making and manufacturing industries.

In 1946 he was transferred to the main office in Cleveland for a special postwar planning assignment. He made an intensive survey of the agricultural economy of the United States and planned an approach to the sale of welding equipment in this market. From this assignment he was promoted to General Sales Manager in 1946.

Mr. Persons is a member of the AMERICAN WELDING SOCIETY, the Sigma XI

Honorary Society and S.A.E. Fraternity. He is a member of the Mayfield Country Club. In college he starred on the track team and keeps trim now with golf and handball. He is married, and has a 12-year-old daughter.

OBITUARY

Joseph J. Charvat

Joseph J. Charvat, Vice-President of the Brook Steel Products, Inc., Chicago, and a member of the AMERICAN WELDING SOCIETY died of a heart attack on Dec. 12, 1950. Mr. Charvat was an enthusiastic booster of welding as a means of fabrication and advanced the idea with all whom he came in contact as well as with his company. He was instrumental in developing and building up the Brook Steel Products, Inc., a contract shop. Mr. Charvat was 42 years old at the time of his death.

A. Cressy Morrison

A. Cressy Morrison, 86, former Secretary-Treasurer of the International Acetylene Assn. and retired executive of Union Carbide and Carbon Corp., passed away at his home at 464 Clinton Ave., Brooklyn, on Jan. 9, 1951. He was also a former president of the New York Academy of Sciences and a former member of the AMERICAN WELDING SOCIETY.

Mr. Morrison made a number of outstanding contributions to scientific advancement. Most of his work was done in connection with the production and safe use of gases, particularly acetylene made from calcium carbide. He discovered a method of separating oxygen and nitrogen in a magnetic field. He was among the first to realize the benefits of air-acetylene flame for assisting plant growth and for home and farm cooking and lighting. He was an early student of the effect of light rays upon animal and vegetable life. Mr. Morrison was also active in securing important measures for improved safety and lighting for miners. In recognition of these contributions, he received a number of honors and, in 1931, on his completion of 25 years of service as its Secretary-Treasurer, the International Acetylene Assn. awarded him the Morehead Medal, for "eminent leadership and administration in the acetylene industry."

Mr. Morrison had a long and varied career as scientist, author and industrialist. His early association with industry was in rubber manufacturing and later in brewing. This led to his service as Secretary-Treasurer of the American Baking Powder Assn. for a number of years, and at that time he published *The Baking Powder Controversy*. He is also author of *Damon and Pythias (Lay Version)*, *The Story of the Man Who Resembled Christ*, *Man in a Chemical World* and *Man Does Not Stand Alone*. The title of this latter work was chosen as a challenge to the conclusions of Julian Huxley in his *Man Stands Alone*. Mr. Morrison also compiled the *Encyclopedia of Superstition* in three volumes.

As a public-spirited citizen Mr. Morrison in 1921 made an extensive report on European wages and living standards before the House Ways and Means Committee. In 1932 he conducted extensive personal investigations into industrial conditions in many European countries. He was unofficial observer for the American chemical industry at the 1928 world economic conference of the League of Nations. In 1931, he returned to Geneva as a delegate of the International Chamber of Commerce. From 1933 to 1937 he was chairman of the Finance Committee of Unemployed Chemists and at that time was also financial director for the Tercentenary of American Chemical industries. In 1937 he was president of the R.O.T.C. Assn. of the United States. In that year he retired from 10 years of service as Chairman of the Executive Council of the American Tariff League.

Mr. Morrison was a member of many national and international organizations including the American Academy for the Advancement of Science, the American Chemical Society, the Electro-Chemical Society, the American Institute of Chemists, American Institute of Mining and Metallurgical Engineers, the Tariff Commission of the National Association of Manufacturers, the Merchants Assn. of New York, the International Fixed Calendar League, the Compressed Gas Assn., the Manufacturing Chemists Assn. of the United States, the Board of Governors for the Synthetic Organic Chemicals Assn. and the Royal Institute of Great Britain.

Mr. Morrison was a fellow and Past-Chairman of the Committee on Astronomy, and he was Chairman of the Advisory Committee of the Hayden Planetarium at the American Museum of Natural History, New York. His interest in astronomy also extended to membership in the council of the Amateur Astronomer's Assn. In 1938, Mr. Morrison was was elected to the New York Academy of Sciences, the oldest scientific society in New York City. His clubs included the Union League Club, the Town Hall Club, the New York Chemists Club, the Cosmos Club, the National Press Assn. and the Congressional Country Club in Washington, D. C.

Although Mr. Morrison resigned as Secretary-Treasurer of International Acetylene Assn. in 1931, he remained an active member until his death. He was always keenly interested in helping to promote the welfare of the industry and its members.

ONE PIECE LEATHER APRONS
give more protection!
COLONIAL GARMENT KIP SPLITS
• WILL CUT ANY SIZE ONE-PIECE APRON
• FULL CHROME TANNED LEATHER FOR HEAT RESISTANCE
• PEARL COLOR—WEIGHT TO SUIT —WELL TRIMMED
Mr. Safety Engineer: We do not manufacture safety clothing, but you can specify one-piece leather aprons made of Colonial Garment Kips from your supplier.
COLONIAL TANNING CO., INC.
Glove Leather Division
730 W. VIRGINIA STREET MILWAUKEE 4, WISCONSIN

NEWS OF THE INDUSTRY

The Bastian-Blessing Co. Has Outstanding Safety Record

The Bastian-Blessing Co. has continued to maintain its outstanding safety record by completing the year 1950 with a total of over 1,701,845 man-hours free from any serious accidents. This is based on the records of 779 employees in the Chicago plant.

In keeping with the established safety practices maintained within its own plant, The Bastian-Blessing Co. and its employees are proving to the LP-Gas Industry that they are practicing the safety principles that they have been preaching to the trade for many years.

Since 1945, The Bastian-Blessing Co. has built up a total of over 6,178,000 man-hours free from any serious accidents. This makes the company one of the most outstanding manufacturing plants in the country from a safety standpoint based on the number of lost-time accidents per million man-hours worked. Past recognition for previous safety performances has been given The Bastian-Blessing Co. by the United States Dept. of Labor.

ABS Meeting

The 89th Annual Meeting of the Board of Managers and Members of the American Bureau of Shipping was held Jan. 30th in the Bureau's board room, 45 Broad St., New York City.

J. Lewis Luckenbach, Chairman of the Board, presided and expressed to more than 60 managers and members his appreciation of their attendance.

Mr. Luckenbach was re-elected Chairman of the Board of Managers and Walter L. Green was re-elected as President.

Re-elected as Honorary Vice-Presidents were Joseph W. Powell and William D. Winter.

David P. Brown was re-elected as Vice-President and Chief Surveyor, and Jerome B. Crowley was re-elected Secretary-Treasurer, and Kennth D. Hull, Assistant Secretary-Treasurer.

We are reproducing below a few brief abstracts from the remarks of Mr. Luckenbach.

Bureau Activity

"There now exist in class with the American Bureau of Shipping 8528 vessels of 36,267,186 gross tons, of which about 20% are temporarily inactive. To this figure there will be added 359 vessels of 1,231,400 gross tons currently under construction in United States and foreign shipyards, making a grand total of 8887 vessels of 37,498,586 gross tons. While a substantial percentage of these vessels are foreign owned and/or registered, the large majority fly the American flag. These figures include seagoing tonnage, Great Lakes and river craft, both self-propelled and nonpropelled. During the past year a considerable number of existing foreign-owned vessels were classed by the Bureau.

"A total of 33 new vessels were completed to Bureau Class in foreign shipyards during 1950, these aggregating 159,037 gross tons. The majority of these vessels were constructed in Japan, but included two in Trieste, one in the Netherlands West Indies, one in Canada and one in Holland. In addition, considerable repair and alteration work on existing vessels was accomplished in foreign ports under Bureau supervision. A large increase was experienced in the number of surveys conducted by the Bureau's foreign exclusive offices, as well as by nonexclusive surveyors."

Technical Activities

"During the past year meetings of the various Technical Committees and specialized subcommittees have been held for the purpose of discussing modifications to the Rules which have been indicated as being desirable, based upon the service records of the various types of vessels and machinery in Class with the Bureau, and to bring the Rules up to date with modern trends in design. As a result of these discussions, several changes to the 1950 Rules have been approved for inclusion in the 1951 edition which will soon be available for distribution. These changes include some minor alterations in the Rules for hull construction, one being in the method for determining the scantlings for the structure bonding deep tanks. Several changes in the requirements for steering gears and for the construction of machinery have been made and the sections on welding, boilers and piping have been brought into agreement with the latest provisions of related Codes and Practices. A number of changes have been incorporated in the Electrical Section, which has been completely rewritten and rearranged for clarity.

"At the meetings of the Committees dealing directly with Materials, Welding and Engineering design problems, and at the regular meeting of the Technical Committee there was submitted for discussion a procedure for repairing fractured shafts by welding. This procedure has been in operation under the auspices of the Navy Department for some years past and records of satisfactory performance of shafts so repaired have been made available to the Bureau. It was agreed by the several Committees that, in view of this satisfactory experience, the Bureau would be justified in accepting welded repairs on fractured shafts where specifically requested by owners, and where done in shipyards or other plants who would become properly qualified under the same elaborate qualification procedure and system of control as has been established by the Navy Department. Instructions as to the requirements for qualification have been sent out to the Surveyors so that they can discuss these requirements with any plants which might be interested in obtaining the Bureau's approval for doing this kind of repair. It is believed that this action by the Bureau may prove to be of considerable value in that it will enable shafts, which have been considered as unfit for further service on account of fractures, to be salvaged and again placed in service where owners may desire to do so.

"Continued research work, not only by the Bureau's own staff but in association with a wide variety of research projects in the field of materials, has continued to support the position which was taken by the Bureau in 1947 in revising its specifications for hull steel plates over $^1/_2$ in. thick. The continually increasing service records of vessels in which material, in accordance with the new specifications, has been used have not disclosed any fractures of the nature which gave rise to the investigations which, in turn, led to the adoption of the new specifications. These specifications were adopted in conjunction with the American steel-making industry and there have been no serious difficulties of application in this country.

"However, in some of the steel-producing areas in other parts of the world, literal adherence to these specifications may be difficult on account of scarcity of materials and in some cases it may not be necessary due to differences in the nature of the ore and scrap and to variations in the manufacturing and rolling procedures. Samples representative of ordinary shipbuilding steel plates produced in accordance with standard specifications existing in those areas, as well as samples of special quality plates which may be offered as substitutes for our Grades B or C plates, are being obtained and subjected to a complete program of testing in our laboratory with the result that these difficulties are rapidly being overcome.

"There has been put into service by the Atlantic Refining Co. the first of the vessels utilizing high-temperature steam, 1020° F., installed in accordance with the Bureau's 'Interim Guide for the Installation of High-Temperature Steam Piping.' Reports after several trips indicate that the installation is working out satisfactorily and with gratifying results in fuel economy.

"In spite of the very few new contracts for construction in the costal shipyards of the United States during the past year, plan approval work has been quite active. The new construction program on the Great Lakes, which was started in the latter part of 1950, has resulted in a large amount of plans being submitted for approval for these vessels. In addition,

there has been considerable planning in connection with the possible conversion of several types of seagoing vessels for use on the Great Lakes, and for the fleets which will be necessary as a result of the development of ore deposits in Venezuela and Labrador, all of which has resulted in considerable work for the Technical Staff. Developments in the field of specially designed vessels for unusual types of cargoes, such as liquefied petroleum products, chemicals, etc., have been continuing. Noteworthy among these is the recent completion of a barge in which there were installed what are termed 'multicylinder' tanks for the carriage of liquefied petroleum products. These tanks consist of sections of cylinders combined to form an approximately rectangular tank having multispherical ends. Such an arrangement permits of the utilization of a far greater amount of space for the cargo within the barge itself than was hitherto possible with cylindrical tanks."

New Shipbuilding Under Way

"All of the large vessels now being built in the United States are to be Classed by the Bureau. These include 15 seagoing vessels of 249,452 gross tons and 14 vessels of 153,370 gross tons designed for Great Lakes service only. Of the latter group, 11 are large bulk cargo carriers, all well over 600 ft. long, two of which will be self-unloaders. One of these bulk carriers will be 690 ft. long overall. It will be the longest cargo ship in the world and will only be exceeded in length by some trans-oceanic passenger liners. Only a few new shipbuilding contracts are in prospect at present for American shipyards, with the exception of the 50 high-speed cargo ships which are to be ordered by the Maritime Administration.

"More than 200 miscellaneous river and harbor craft of over 100,000 gross tons are at present being built to Bureau Class in the United States. The great majority of these vessels are designed for service on the extensive inland waterways systems of the United States. This class of shipbuilding has continued in good volume during each of the postwar years."

United States Shipbuilding Production in 1950

"All but three of the tankships completed in 1950 are of the supersize class, ranging from 26,750 deadweight tons up to 30,155 tons. The three exceptions were tankers of 16,560 tons. As a result of the trend in the postwar years by American tanker operators to increasingly larger bulk oil carriers, primarily for long trans-oceanic hauls, shipyards of the United States have now completed a total of 51 of these new giants of the seas. Five more are still being built. It is of interest to note that oil producers in other countries have since found it advantageous to also order supersize tankers. As a result, there were completed in the last half of 1950 a total of 8 of these vessels in Sweden and the United Kingdom. Many more are being built in British, Swedish, French, Italian and Japanese shipyards.

"Also completed to Bureau Class in 1950 were 222 miscellaneous small craft, both self-propelled and nonpropelled, totalling

PAGE *Stainless Steel*

WELDING ELECTRODES

Armor Welding Electrodes
. . . and All Standard Types
AC or DC . . . the BEST
. . . for any weld
on all types
of stainless steel

STAINLESS STEEL has great strength . . . but its strength is limited by the quality of welds which seal the joints. In all kinds of manufacturing—military work in particular—stainless is being used more widely. If you weld stainless, choose electrodes with care.

PAGE STAINLESS STEEL ELECTRODES, AC or DC, give a stable arc under all conditions. The metal flows smoothly. Slag is clean and easily removed. The coating resists cracking down to very short stubs. Now available in 10-lb. lined, hermetically sealed metal cans which can be reclosed. ***Be sure . . . specify Page Stainless Steel Electrodes.***

ACCO

Manassen, Pa., Atlanta, Chicago, Denver, Detroit, Los Angeles, New York, Philadelphia, Portland, San Francisco, Bridgeport, Conn.

PAGE STEEL AND WIRE DIVISION
AMERICAN CHAIN & CABLE

PAGE WELDING ELECTRODES

112,239 gross tons and 196,700 deadweight tons, and constructed principally by the many fine shipyards on our inland waterways."

Privately Owned Fleet

"The privately owned tankship fleet of 426 units now totals 4,157,725 gross tons and 6,618,695 deadweight tons. There will be added early this year two more oil tankers of 30,155 tons each, these being at present the largest of their type in the world and the first merchant vessels to utilize in their propulsion plant steam at a temperature of 1020° F. The first of these three sister ships, the *Atlantic Seaman*, built for the Philadelphia Tankers, Inc., by the New York Shipbuilding Corp., went into service late in 1950 and preliminary reports indicate very successful operation.

"The trend in recent years in the transportation of bulk commodities is definitely toward larger units, as evidenced, first, by the construction at the end of the war of 24,000-ton bulk ore carriers, and more recently the supersize tankship. This trend is being followed in other countries and, also, on the Great Lakes. The limiting factor as to size of the units is principally the draft of water available in the harbors and on the route. However, the vessels now under construction are by no means the largest possible for their particular service, and the probabilities are that the current huge seagoing bulk ore ships and tankships will in the near future be superseded by much bigger vessels."

Conclusion

"We have again been brought face to face with the necessity of having sufficient United States tonnage available at all times for national defense. If we must continue to fight such wars as may be forced upon us on foreign soil, the maintenance of an adequate merchant fleet is our foremost requirement.

"In view of the improvements made in performance of submarines, should actual sea warfare become imminent higher speed for cargo ships would become of utmost importance. With a convoy system of slow speed ships, the hazards and losses might be expected to be extreme. The higher speed ship is free of many of these hazards and she need not travel in convoy. The planning by the Maritime Administration and the military establishment of larger vessels with sea speeds in excess of 20 knots is indeed a prudent step. It is noteworthy to observe that even in the Korean situation, where we have not been faced with naval warfare, the vessels chartered from private owners and those drawn from the reserve fleet for the supplying of our forces have been vessels of the speedier type. Not one of the low-speed, Liberty-type cargo vessels has been removed from the reserve fleet for active war service. This seems adequate proof of the obsolescence and unsuitability for present use of that large reserve of this type of vessel. It is interesting to note in passing that the ship sales and charter program at present has returned to the United States Treasury over two billions of dollars representing almost half of the original cost of the vessels sold. Certainly no other type of war expenditure can boast of a similar record.

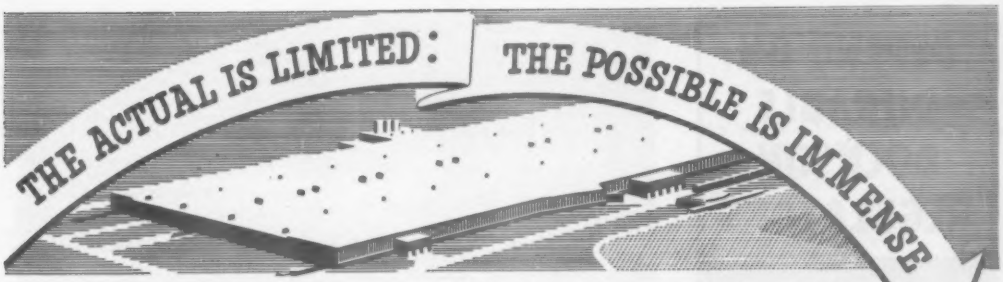
"Today new construction in the coastal shipbuilding yards of the country is at a low ebb. The placing of new contracts, so necessary to keep these yards active and the personnel required to operate them in case world conditions become more serious, is most urgent. The maintenance of a well-balanced merchant marine of high-speed ships and, correlated with this, the maintenance of the shipbuilding and repair yards is vital to our country. Any emphasis which we place on this could not be too great. We have had so many lessons already. Money spent in times of peace for the maintenance of a merchant marine pays off a hundredfold in times of stress".

New Welding Courses

A continued program of new classes designed to instruct welders in the latest metal-joining techniques was announced recently by Eutectic Welding Alloys Corp. of New York.

The Eutectic Welding Institute, located in Flushing, N. Y., will offer a new series of week-long classes to men of the metals industries. These classes are scheduled for the weeks of March 12th, April 16th and May 14th of this year. Registrations are now open.

The course of instruction, under the guidance of skilled technical men, includes



NEW LINCOLN PLANT CREATED BY INCENTIVE-INSPIRED CO-ACTION IN DEVELOPING POSSIBILITIES IN PRODUCT

© LE Co. 1951

Machine frame designed in welded steel maintains alignment to .005"

Textile machine bed fabricated at The Warner and Swasey Company, Cleveland, Ohio, shown cast steel adapter welded to end of frame.

BY redesigning to a simpler, more economical welded steel construction, this prominent machine tool and textile equipment builder now maintains closer production tolerances while cutting the machining time from 8 hours to 2¼ hours. Alignment accuracy, held within .005" and squareness to within .0015 on the 120 inch frame members shown, is maintained with only one roughing and one finishing cut.

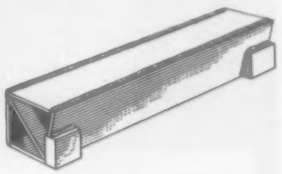
Originally the frame was fabricated from sheared plate using continuous corner welds. The present, simplified design incorporates steel tubing and four angles to which steel castings are welded at each end for subsequent assembly on side frame members.

In addition to saving 170 pounds of metal on each frame, welding time has been cut in half and stress relieving eliminated.

Welding engineers at Lincoln will gladly show you how to design your products in welded steel to improve performance and cut your manufacturing costs. Simply write or call.

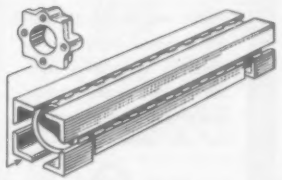
HOW TO DESIGN FOR WELDED STEEL. Latest engineering data, techniques, speeds and costs are presented in the new 9th Edition Procedure Handbook of Arc Welding Design & Practice. Price only $2.00 postpaid in U. S. A.; $2.50 elsewhere.

the ACTUAL



Original Design *of bed frame for textile machine was difficult to machine accurately . . . required stress relieving.*

increasing the YIELD



Present Design *takes 21% less metal . . . 50% less welding . . . eliminates need for stress relieving.*

the IMMENSITY of the POSSIBLE

A 72% REDUCTION in machining time

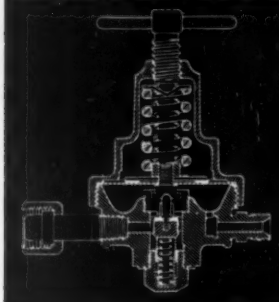
SEE HOW STEEL DESIGN INCREASES ACCURACY

Machine Design Sheets free on request to designers and engineers. Write on your letterhead to Dept. 93,

THE LINCOLN ELECTRIC COMPANY

CLEVELAND 1, OHIO

DEPEND ON DURABLE DOCKSON REGULATORS



in types for Oxygen, Acetylene, CO_2, Air, Fuel Gases.

"BUILT FOR BETTER SERVICE" with improved diaphragms, replaceable, self-aligning seats; sturdier construction; shatter-safe gauges; alert safety valves and other advantages.

THERE IS A DOCKSON DISTRIBUTOR NEAR YOU—Let us send you his name and our complete catalog of DOCKSON WELDING and CUTTING EQUIPMENT.

Dockson CORPORATION
3839 WABASH • DETROIT 8, MICH.

practical applications in the joining, welding, brazing, overlaying, and cutting of virtually all metals used in the nation's vital defense production program.

This practical training, supplemented by lectures, slides, films and demonstrations concerned with cutting production costs and time losses, will prove especially valuable to those firms involved in defense work at the present time as well as to maintenance and repair men throughout the metals field.

The Eutectic Welding Institute is a nonprofit venture and practically the entire cost for its operation is sustained by the sponsoring company. Indeed, for members of the armed forces, all courses are given without any charge whatever.

For further details, course outlines, schedules, etc., write to Educational Dept., Eutectic Welding Alloys Corp., 40 Worth St., New York 13, N. Y.

Welded Repair Boat

Archie Lang, president of Inland Seas Boat Co., 3457 W. 140th St., Cleveland 11, Ohio, announces that his company has just completed an all-steel work boat for the Boom Boiler and Welding Co. of Cleveland.

The boat was designed by John B. Clark, naval architect, to be used in the maintenance and repair work of lake freighters the year round. It is 41 ft. long, having a beam of 13 ft. and hull weight without equipment of approximately 7 tons. The bottom is constructed of $^1/_4$-in. steel plate with topsides of $^3/_{16}$-in. steel plate securely welded into position.

Heavy construction throughout affords ample protection and safety against the encounterment of ice and heavy seas. An all-steel pilot house fore, and an all-steel deck house aft, afford all-weather comfort for the crew and protection of equipment.

The Inland Seas built boat is powered with a 150 hp. Chrysler marine engine having a $4^1/_2$ to 1 reduction gear. A large air compressor for the operation of pneumatic tools, gas-welding equipment and two large gasoline-driven arc welders have been installed.

The entire hull, including rub rails, bulkheads and deck framing was built in an upside-down position and turned over for the completion of the deck and pilot house.

This method of boat construction is a unique development in boat building of this size. It is the same method utilized by the Inland Seas Boat Co. in building pleasure boats. The system readily effects a speed-up in production that results in the building of more boats at a much lower cost per unit.

Although still in production on their plastic-steel hull 18-ft. Harbormaster, and 23-ft. Steel Clipper cruisers, the Inland Seas Boat Co. has increased its capacity for the design and construction of heavy-duty work boats, tugs, fishing boats and other boats according to specification.

Flame-Cutting Turnbuckles

Oxyacetylene cutting is being used by a Tacoma, Wash., firm to rough out turnbuckles from blank bar on a production basis. The cut bars are then heated in a furnace, spread apart by wedge-shaped dies, and threaded for eye bolts.

Fig. 1 This jig is used to rough out turnbuckles from blank bars with oxyacetylene cutting. The operator is watching two Oxweld C-56 blowpipes, equipped with two-nozzle adaptors, cut four bars. Cut bars are piled in the foreground

Marine Iron Wks. of Tacoma does the cutting automatically with two blowpipes mounted on a cutting machine. Each blowpipe is equipped with a two-nozzle adaptor so that four cuts are made simultaneously.

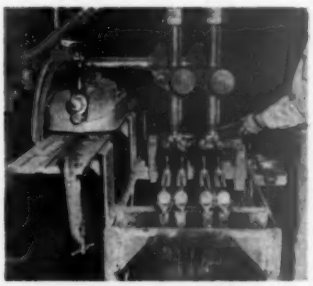
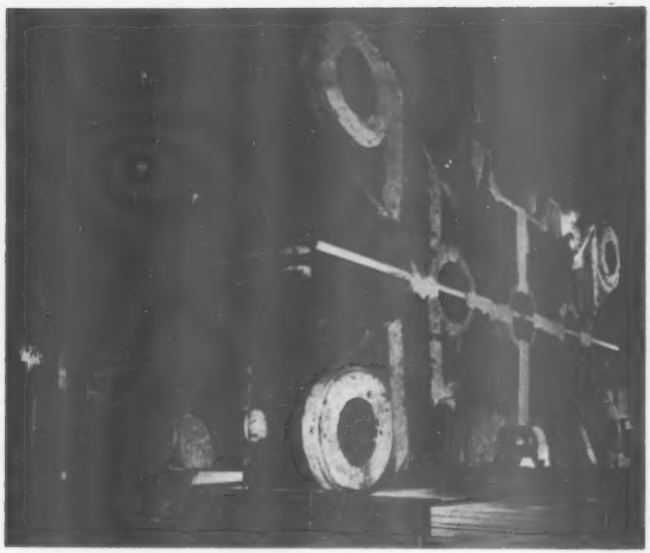


Fig. 2 The operator unloads four cut bars from a jig used to make turnbuckles as four more bars are being cut by two blowpipes mounted on an Oxweld CM-30 cutting machine

A special jig, which holds eight lengths of $1^1/_2$-in. round bar, is used for the cutting. The 22-in. lengths are set in the jig in two rows of four bars. The first four bars are cut, then while the second group is being cut the operator unloads and loads the other half of the jig. Then the process is repeated so that it is a continuous operation. Cutting time for the eight bars, including down time for flame adjustment and nozzle cleaning, is only five minutes.



Press Welded

A welder is shown above joining edges of crown being constructed for an 8000-ton triple action hydraulic press.

The crown, resting on its side at the Bethlehem, Pa., plant of Bethlehem Steel Co., is 29 ft. long, 12 ft. 10 in. wide, and over 9 ft. high. It is made of rolled-steel plates and castings, about 6 in. thick, welded together. The metal was preheated with gas burners to avoid cooling strains after welding.

An aircraft company will use the big press in forming operations.

A Weld

A weld is a weld. . .and when made rigid is as strong or stronger than the plate in which it is made. When a mile of this 30-in. pipeline exploded, the welds, both longitudinal and circumferential, held while the plate tore apart as if cut with shears. The solid welds can be clearly seen in the section of pipe in the top of the picture. (Acme Photo, Courtesy Lincoln Electric Co.).

(Below) Pipe after explosion with welds intact.

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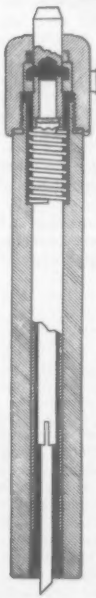
RESISTANCE WELDING
HOLDERS

- Water Flow
- Conductivity
- Simplicity of Design
- Simplicity of Maintenance
- Rust Proof
- Durability
- Non-Magnetic Throughout

Summary

WW O.K. on all counts



WEIGER-WEED

ELECTRODE HOLDERS

Weiger-Weed Ejector Type Holders represent the closest approach ever made to an absolutely trouble-free resistance welding holder. Designed and built for the most severe conditions in resistance welding, they have proved themselves in thousands of hours of failure-free service. Weiger-Weed & Company, Division of Fansteel Metallurgical Corporation, 11644 Cloverdale Ave., Detroit 4, Michigan.

Send for this free booklet of latest information on Resistance Welding

WEIGER-WEED

12802-A

NEW LITERATURE

Stainless Steel Story

"A Stainless Profit Story for the Paper Industry" is the title of a new, illustrated booklet published by Armco Steel Corp. It tells how stainless steel equipment eliminates corrosion problems, lowers maintenance costs and improves stock quality. And especially valuable in view of current conditions, it gives practical suggestions for the profitable operation and care of stainless steel equipment.

Copies of the booklet can be obtained from Armco Steel Corp., Middletown, Ohio.

Jigs and Fixtures for Inert-Gas Arc Welding

A new reprint, "Jigs and Fixtures for Inert-Gas Arc Welding," has been announced by Air Reduction Sales Co., a Division of Air Reduction Co., Inc.

This 11-page article, which originally appeared in THE WELDING JOURNAL, was written by H. A. Huff, Jr., and A. N. Kugler and discusses the need for jigs and fixtures. The necessity to design these fixtures for the newer welding processes involved as well as for the product turned out is covered.

This article employs 26 photographs and sketches to illustrate the principles discussed and to show some application of these principles.

Metco News

The current issue of the *Metco News* describes and illustrates several good metallizing salvage applications. These salvage jobs have saved the metallizing users thousands of dollars in materials and production time. Scarce metals were also saved. For instance, a large automotive manufacturer has a production salvage metallizing setup to save defective motor blocks. Previously many of these blocks were scrapped because of porosity and consequent leakage of cooling water. Now they are grit-blasted, metallized with 0.020 in. of copper, followed with a flash coat of Sprairon for appearance. In extreme cases where the leaking area is actually spongy, it is preheated with a torch to 400° F. before spraying it with copper. When the cast iron cools, it shrinks around the copper, making it absolutely impervious to water. Other production salvage casting jobs have now been set up because of the success of this application.

Write for your copy of *Metco News*, Vol., 5 No. 6, to the Metallising Engineering Co., Inc., 38-14 30th St., Long Island City 1, N. Y.

Introduction to Mechanical Design

Introduction to Mechanical Design, by T. B. Jefferson, Editor, *The Welding Engineer*, formerly Design Engineer, Office of Chief of Engineers, U. S. Army, and Walter J. Brooking, Special Projects Department, M. W. Kellogg Co., formerly Dean, LeTourneau Technical Institute. This book consists of 612 pages and is published by The Ronald Press Co., 15 E. 26th St., New York 10, N. Y. Price $6.50.

In preparing this book the authors have had as an objective a textbook which not only would have a completeness from the practical viewpoint which is lacking in the usual machine design textbook, but also would follow much more closely the mode of creative thinking of engineers who are actually designing machines, rather than simply considering their elements as individual and separate theoretical problems.

There is a whole chapter of 36 pages devoted to Welded Fastenings and Joints and reference to welding in other portions.

Hobart Arc-Welding News

Volume VII, No. 3, "Hobart Arc Welding News," a 24-page booklet of interesting photographs and articles on welding from all over the country, is now available. Copies are mailed free of charge to anyone interested in arc welding.

Many of the articles feature time- and money-saving applications. To get your copy, write the Hobart Bros. Co., Troy, Ohio.

Aluminum Data Book

A new 194-page publication, *The Aluminum Data Book*, has just been issued by Reynolds Metals Co., Louisville, Ky., to meet the demand for more factual information on aluminum alloys and mill products.

Aluminum is no longer a single metal but a large family of metals, each with its own peculiar properties. To make an intelligent selection, the user must have definite information as to alloys, tempers, sizes, shapes, physical properties, chemical properties, mechanical properties and fabricating characteristics.

These vital facts on aluminum alloys and mill products have been condensed into a handy pocket-size manual which contains 117 tables of data on physical, chemical and mechanical properties; standard tolerances; weights; standard sizes and production limits; as well as much fabricating data. Included are tables showing relative corrosion resistance, the action of many chemicals on aluminum, elevated and low-temperature properties, fatigue strengths, minimum bend radii, joining methods, finishes for aluminum and much other information.

In addition, there are 33 pages of explanatory text covering a wide range of related subjects such as the alloy designation system, the temper designation system, heat-treatable and nonheat-treatable alloys, casting alloys, casting methods and foundry practice. Wrought aluminum mill products are detailed and manufacturing methods described. Fabricating methods for the wrought alloys are discussed, including blanking and forming operations, machining, forging, joining and surface finishing.

In addition, the back of the book carries a complete detailed cross index covering both tabular and text material.

This book is a revision and enlargement of a similar book issued by Reynolds in 1948. The new book carries 32 more pages with much additional data covering new alloys not then in use.

Wire-bound to permit flat opening, this 6 x 9-in. book also contains 61 illustrations showing various operations in the production of aluminum. It will be sent without charge to engineers, designers and technical men who request it on company letterhead. Address Reynolds Metals Co., 2500 S. Third St., Louisville, Ky.

Bulletin on Weld Spatterproofing

G. W. Smith & Sons, Inc., 5400 Kemp Rd., Dayton 3, Ohio, announce a new 4-page bulletin on *Protect-O-Metal* weld spatter preventives and metal protection compounds, with full details on composition, application and potential savings provided by three grades (Nos. 2, 7 and 8) of Protect-O-Metal.

In addition to its spatterproofing properties, the relatively new Protect-O-Metal No. 8 provides corrosion protection for long periods which is of special value to manufacturers and fabricators who may store metal stock, semifinished materials, dies, etc., out of doors prior to welding operations. It dries quickly to a hard thin film which protects against dirt, moisture, corrosion and prevents adhesion of weld spatter. It is excellent on stainless steel, and need not be removed from any surface prior to painting.

Copies of Bulletin, No. 50 are free on request.

NEW PRODUCTS

Spot-Welding Control Equipment

New synchronous and nonsynchronous control equipment for low-capacity, spot-type resistance welding machines is available for Westinghouse Electric Corp.

Air-cooled thyratron tubes make and break the welding current, with no moving parts. All components in each equipment are mounted on a side-swinging panel, enclosed in a N.E.M.A. type I enclosure for ease of inspection and maintenance. The units can be mounted on or near the welding machine. Knockouts are provided in the enclosure to facilitate making external electrical connections.

Synchronous units can be supplied in rms. current ratings up to 50 amp. at a 10% duty cycle. Nonsynchronous units are available in ratings up to 100 amp. at 10% duty cycle.

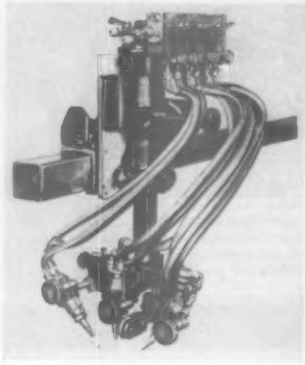
For further information, write Westinghouse Electric Corp., Box 2099, Pittsburgh 30, Pa.

Plate-Edge Preparation Device

Air Reduction Sales Co., a Division of Air Reduction Co., Inc., has announced the availability of a new Plate-Edge Preparation Device.

This Plate-Edge Preparation Device has been designed to increase production and insure clean-cut, accurate preparation of plate edges. Its ability to cut a single or double bevel accurately, with or without a land, recommends it especially for use in shops or factories which do any amount of steel fabrication work.

The Airco Plate-Edge Preparation Device, which employs a spring-balanced, free floating carriage and caster-wheel assembly to permit bevel cutting over plate undulations while maintaining a constant tip-to-work distance, may be mounted on any gas-cutting machine equipped with a 3-in. square torch bar.



Torches may be individually positioned vertically or laterally without changing the bevel angle. Fuel and preheat pressures are initially set with individual torch valves, and once set, the master valve controls turning-on and shutting-off gas supply without disturbing settings of the individual torch valves.

For further information about this new Plate-Edge Preparation Device, write to the Airco Sales office nearest you or Air Reduction, 60 E. 42nd St., New York 17, N. Y.

Arc Welder

A special 300-amp., gasoline engine driven arc welder with a 3-kw. auxiliary power generator, popular with the armed forces as a field repair and maintenance unit, is being announced by the Hobart Bros. Co., Troy, Ohio.

This arc welder and power generator is a self-contained unit, powered by a 6-cylinder, self-starting Chrysler Industrial Engine directly connected to the welding generator and mounted on a welded steel frame. An auxiliary 3-kw. power generator provides for lights and such tools as a lathe, grinders, drills, brake reliner, valve refacer, chipping hammers, power wrenches and paint sprayers. The unit is completely enclosed by a sheet-metal canopy that is bolted directly to the frame. Hinged side doors provide access to the control panels, the engine, the welding generator and other parts within the canopy.

The welding generator is rated at 40-v., 300-amp. under 1 hr. resistance load at 1500 rpm. and has a current range from 50 to 400 amp. Cooling is accomplished by a large squirrel cage type fan which draws cool air in at both ends of the generator and expels the heated air at the center.

On the left side of the engine control panel are located the controls and instruments that have to do exclusively with the control of the auxiliary power generator, These are a d.-c. ammeter, d.-c. voltmeter. auxiliary power control rheostat, circuit breaker, one set of 6-v. terminal studs, two sets of 115-v. double receptacles and one set of 115-v. terminal studs.

On the other panel are located the controls and instruments that have to do exclusively with the control of the welding generator. These are the multirange switch, the volt-amp adjuster, the "ground" and "electrode" cable terminals, a reverse polarity switch, d.-c. voltmeter and d.-c. ammeter.

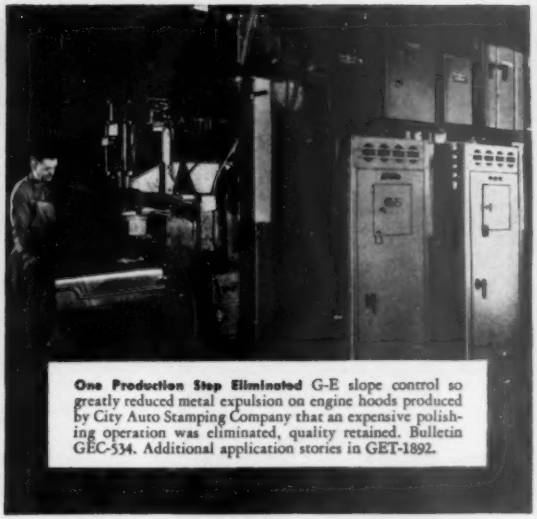
Ground Clamp

Erico Products, Inc., Cleveland, recently introduced their new Caddy Ground Clamp, 500-amp. capacity. This addition to the arc-welding industry has a pressed all-steel body. Outstanding features of the Caddy Ground Clamp are:

1. The cable is welded to the jaw. The connection has a permanent high conductivity and cannot burn up under excessive overload.
2. The body of the clamp is entirely insulated from the contact jaw preventing arc damage to the clamp.
3. The replaceable contact jaw is a high-conductivity copper casting.
4. The cable gripper ears permanently hold the cable just behind the weld. This prevents the usual flexing and fraying of the uninsulated area and greatly increases the life of the cable at the weld.
5. The torsion spring that maintains pressure for a constant grip is completely housed by the steel body of the ground clamp.

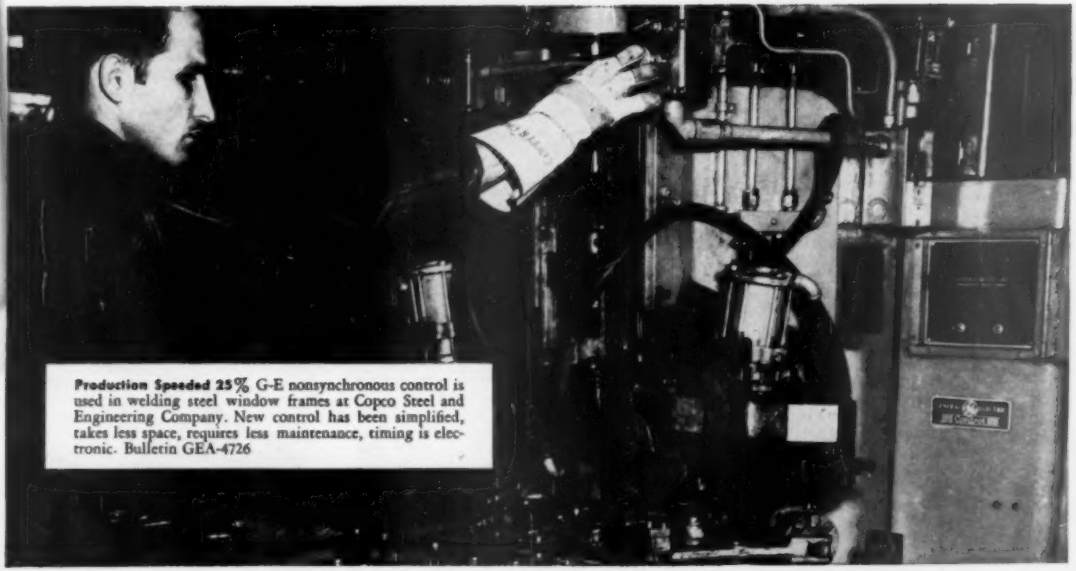
An instruction sheet for welding the cable to the Caddy Ground Clamp by the Cadweld Process is available at the present time.

New Alloy Welded—just .007 inch thick G-E Thyratron spot-welding control is used at the Elgin National Watch Company on a Dura Power mainspring. Elgin reports, "Thanks to G-E control, work is easy, accurate, and production is high." Bulletin GEA-4175A.

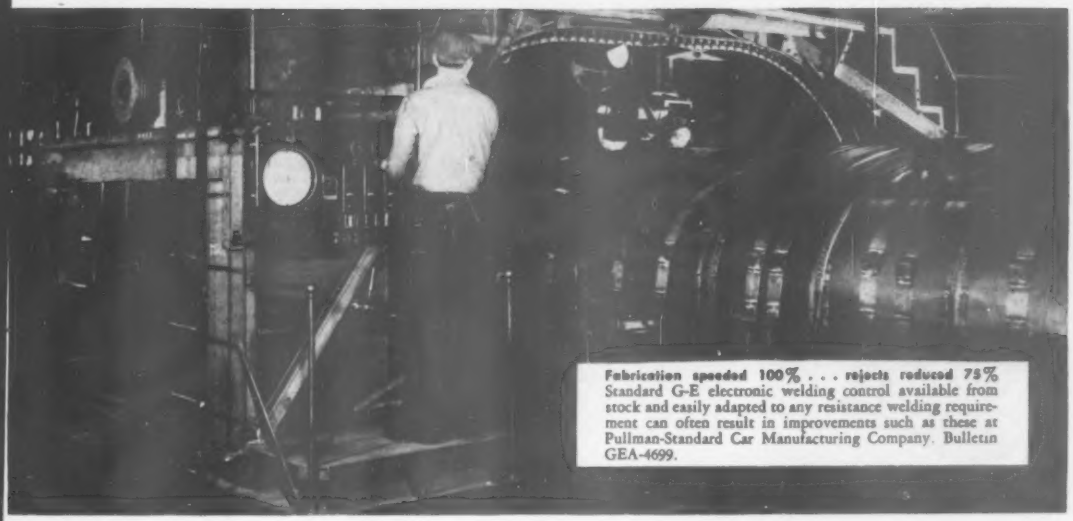


One Production Step Eliminated G-E slope control so greatly reduced metal expulsion on engine hoods produced by City Auto Stamping Company that an expensive polishing operation was eliminated, quality retained. Bulletin GEC-534. Additional application stories in GET-1892.

LOOK!



Production Speeded 25% G-E nonsynchronous control is used in welding steel window frames at Copco Steel and Engineering Company. New control has been simplified, takes less space, requires less maintenance, timing is electronic. Bulletin GEA-4726



Fabrication speeded 100% . . . rejects reduced 75% Standard G-E electronic welding control available from stock and easily adapted to any resistance welding requirement can often result in improvements such as these at Pullman-Standard Car Manufacturing Company. Bulletin GEA-4699.

4 more companies improve production

WITH G-E ELECTRONIC RESISTANCE WELDING CONTROLS

Weld faster, cleaner, and with less kva demand. Weld new alloys. There are G-E electronic controls to help *you* improve production, improve *your* product, reduce costs just as they have done in the cases described here. Take advantage of the newest improvements and developments in G-E control—the wide experience of our welding control specialists. Together with your welding machine manufacturer, they'll help you solve production problems. Write your nearest G-E office today for more information. *General Electric Company, Apparatus Department, Schenectady, New York.*

REMEMBER . . . GOOD resistance welding depends on GOOD electronic control. When you buy insist on G.E. . . . longest in the business with the most complete line of resistance welding controls. For quick and easy training of operators ask your welder manufacturer, power supplier, or the nearest G-E office for a FREE showing of the sound, all-color movie, "This is Resistance Welding," a General Electric More Power to America film.

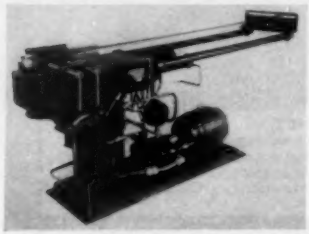
You can put your confidence in—

GENERAL ELECTRIC

645-53

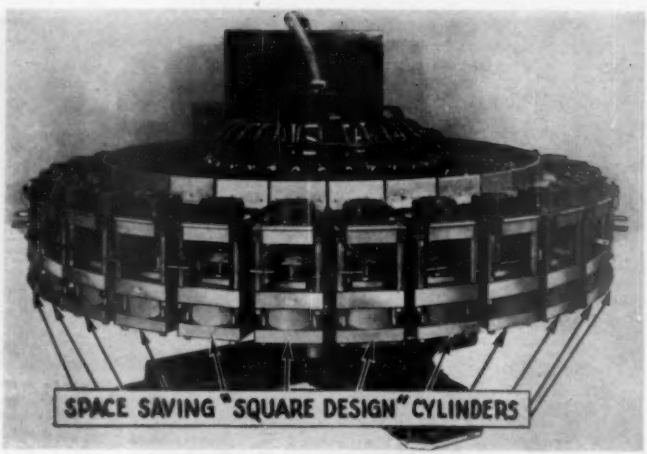
Hydraulic Bender

A new production or custom Wallace Bending Machine Model No. 800 by the Wallace Supplies Mfg. Co., 1300 Diversey Pkwy., Chicago 14, Ill., is this small compact bending machine. It is only 21 in. wide by 34 in. high by 78 in. long for space saving, but with power well beyond its capacity of $1^1/_4$ in. O.D. by No. 16 B.W.G. steel tubing. The unit is powered by a standard 2 hp., 220/440 v., 3-phase, 60-cycle motor furnished with the machine.



It is operated by a single lever which when pushed down causes the bending arm to swing around to the degree of bend selected and stay there until the operator has removed the bent part, then the lever is moved up and the arm swings back to its original position.

It is of all-welded construction, and the 10-in. steel pipe to which the top and base are welded is also the tank containing the hydraulic fluid.



"Cyclewelding" Machine

Designed by Hautau Engineering Co., of Detroit, Mich., for volume production of automotive transmission brake bands, a new special, 30-station "Cyclewelding" machine owes its economical compactness of design largely to the use of space-saving "square-design" cylinders developed by Miller Motor Co., Chicago manufacturer of air and hydraulic cylinders. The design of the machine makes maximum use of standard parts for easy replacement. Each of the 30 stations is, in effect, an independent press with its own, easily replaceable standard thermostatic controls, electrical connections and Miller "square-design" cylinder.

The space saved by the square-design cylinders over conventional type circular bolted designed cylinders permitted the 30 stations of the machine to be designed and built into an unusually compact $9^1/_2$-ft. diameter circular table, allowing convenient accessibility to center slip rings

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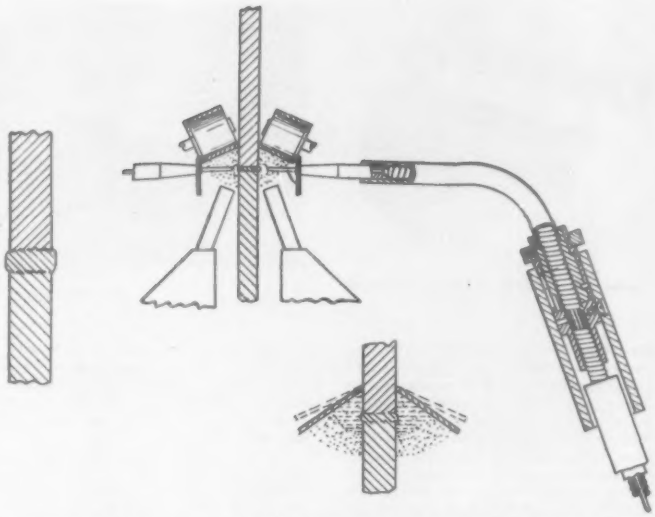
CARBON

DC Welder proves itself again!
Schwartz Supply Company
BUILDING MATERIAL AND SPECIALTIES
November 29, 1950
Gulf Welding Equipment Company,
1133 Magazine St.,
New Orleans 13, La.
Gentlemen:
Several months ago we purchased from you a Westinghouse RA Electric Welder. This is the new type DC Welder with no moving parts except a fan.
We would like to take this occasion to let you know that this machine has been very satisfactory and is all that you promised us it would be. We believe our power saving is at least 50% over what the same capacity motor generator welder would cost us to operate.
With all good wishes, we remain,
Yours very truly
SCHWARTZ SUPPLY COMPANY
BY:
CC: MP
YOU CAN BE SURE.. IF IT'S
Westinghouse
Rectifier Welders

and air headers from a standing position. Conventional type circular "bolted" designed cylinders of the same bore would have required an 18-ft. diameter table, making the machine much more expensive to build, more costly and difficult to service and occupying over three times the floor space.

The new machine is an example of automation, whereby one operator loads the rings and liners, then trips the expander valve lever. The worktable rotates at variable speeds from 3 to 6 min. per revolution depending upon "cure" time required. Cyclewelding operation requires about 450° F. temperatures and 200 to 450 psi. lining area pressures.

In addition to their space-saving feature, the cylinders have solid steel heads, caps and mountings, scratch-resistant, hard-chrome plated piston rods, dirt wiper seals, rustproof precision-honed brass barrels, and self-regulating, wear-compensating seals that are nonadjustable and tamperproof, thereby helping to assure the smooth, maintenance-free operation of the machine. For more information on these cylinders, write Miller Motor Co., 4027 N. Kedzie Ave., Chicago 18, Ill.



Drawing from Patent No. 2,529,812 showing new automatic "3 o'clock" welding process. Horizontal seam in vertical plate being welded from both sides simultaneously. Joint is stationary as two electrodes are moved along opposite sides of work. Flux is carried on a moving belt so that it is stationary in relation to the work

New Welding Process

Automatic hidden arc welding has been limited in application to jobs where the joint to be welded is in position for down-hand welding. A new process, developed and patented by The Lincoln Electric Co., Cleveland, removes this limitation and extends the advantages of automatic hidden arc welding to jobs where the joint is in positions other than that for down-hand welding.

The new process, referred to as "3 o'clock" welding, greatly reduces the cost of welding and expands the possibilities for the application of hidden arc techniques. Because joints can be positioned horizontally, welds from both sides of the joint can be made simultaneously. This reduces actual arc time on a given joint by about 50% and results in lower direct labor costs. Positioning each weld for down-hand welding is eliminated thus reducing handling and set-up time.

In addition to reducing direct labor costs, savings are made because of the smaller sizes of electrode wire used. For example, where on a down-hand application a $^{7}/_{32}$-in. diameter electrode would be used, in a "3 o'clock" position a $^{5}/_{32}$- or $^{1}/_{8}$-in. diameter electrode is used. This means lower currents are used, less electrode is required, smaller quantities of flux are consumed and welds of reduced cross-sectional area are made, thus wasting less metal in unnecessary build-up. Welds can be made in either straight seams or following an irregular contour. Other advantages of the new process are a minimizing of the effects of distortion and the causes of weld cracking. Tendency for burn-through is reduced and back-up strips can be eliminated where two arcs on opposite sides of the work are used.

The process is ideal for applications such as fabricating pipe, box sections, special I-beams and H-sections, joining clips to automobile bumpers, field erection of large outdoor storage tanks, farm machinery parts and other machine frames.

Automatic Hidden Arc-Seam Welder

Machines made for adapting automatic hidden arc-welding heads are usually specialized fixtures of little value except for one or two applications. The Cecil C. Peck Co. of Cleveland has now eliminated this limitation with a welding machine which has a wide range of application and

WELDING CONNECTORS
Saze System Welded Connection Units
for welded assembly

Saze Units place in position and securely hold together structural parts to be welded.
As used in many welded structures they *eliminate all hole punching* producing an economical, rigid, safe and quickly erected structural frame.
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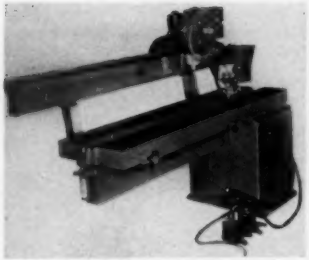
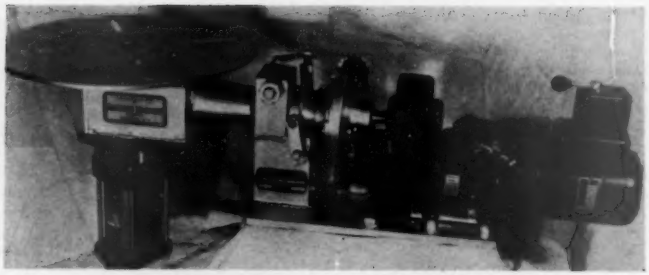


Automatic Hidden Arc Seam Welder



Positioner for Automatic Welding

does not tie capital investment to one particular welding job. The machine being manufactured is a standard seam welder for automatically welding seams in metal ranging from 14 gage to 1/4 in. This universal horn-type welding machine uses a "Lincolnweld" head and carriage for automatic hidden arc welding.

The machine is designed for welding cylinders or other hollow shapes. It can also be used to splice sheets and through-weld flanges in making containers. Work up to 18 ft. long can be handled and the machine can be extended to take longer pieces. Minimum diameter is 12 in. Typical applications where the machine is employed are hot-water heater shells, air-compressor tanks, furnace bodies and water-softener tanks.

Work is placed over the rigid box section horn and the two edges of the seam to be welded are gripped by pneumatically operated heavy copper fingers. Clamping pressure of the fingers is provided by air tubes in a hollow box section weldment on top of the horn.

Backup for the weld is provided by a patented "gremlin" back-up device. An air-operated copper anvil clamps tightly up against the back of the seam being welded. The fingers and back-up anvil clamp the work in exact alignment with a minimum of deflection and variation in area through the joint. The accuracy of the clamping secures tight, straight fit-up which results in a constant cross section of weld and uniformity of quality. Tanks can be fabricated on this machine with a minimum of leakers.

This standard seam welder is a versatile machine and can be adapted to a wide variety of work. It can be used for automatic brazing or inert-gas-shielded arc welding on stainless steel and nonferrous metals. It is a standard machine not limited to a specialized welding operation.

For further information write to Aronson Machine Co., Arcade, N. Y.

Positioner for Automatic Welding

This machine was designed for automatic welding and it features a solenoid actuated clutch that allows instant start and stop of the work table with the advantage being that the welding machine and the worktable can be synchronized to start at the same time one button is pushed.

The worktable rotates at any infinitely variable speed from zero to 5 rpm. and backlash is reduced to a minimum, allowing smooth, jerk-free rotation on Timken Taper Roller Bearings. The worktable can be tilted in any position about 360° and locked. The work arm can be any length to suit up to 16 in.

The model illustrated has an air cylinder attached to the table spindle. This provides that a pull-bar attached to the cylinder through the hollow spindle will permit clamping the workpiece to the table. The advantage of the holding device is the speed with which the workpiece can be attached to the table.

The model 14BT500 is ideally suited for any automatic, semiautomatic, manual arc, heli-arc, brazing, etc., applications that require a smooth rotation with instant start-and-stop synchronized worktable rotation. The weight capacity rating is 500 lb.

Furnace Brazing

Induction and furnace methods of brazing are greatly simplified by the availability of suitable brazing compounds to replace rings or strips.

Of especial importance to firms engaged in defense production is the recent announcement by Bent Laune, president, All-State Welding Alloys Co., Inc., 273 Ferris Ave., White Plains, N. Y., that his company is now in a position to supply bronze ground to specified mesh and mixed with flux.

The resultant brazing compound will be designated as All-State No. 45 Brazing Compound (Specified Mesh). It will be a light gray powder to be mixed with water before application. It will flow at about 1800° F. and produce a smooth thin yellow alloy with fillets of similar size to those of silver solder. The alloy will run completely through the joint and will develop a shear strength of 80,000 psi.

Transformer Arc Welders

The new 300-, 400- and 500-amp. Wilson Bumblebee arc welders, available as indoor or all-weather models, have been announced by Air Reduction Sales Co., a division of Air Reduction Co., Inc.

All models have the sturdy construction, compactness and light weight so important to high-quality, economical welding. Outstanding features include: instant arc-starting, wide current range, easy-to-operate controls and minimum maintenance.

Automatic "Hot-Start" control with arc-stabilizing capacitors are provided on the 300- and 400-amp. models. Current ranges are 60–375 for the 300-amp. model, 80–400 for the 400-amp. model and 100–675 for the 500-amp. model. The Silicone insulation means safety two ways: it operates safely at high temperatures without breaking down and is water repellent. Also, the light weight resulting from the use of Silicone insulation makes these models ideal to mount on running gear for portability.

For further information, communicate with your nearest Airco office or write to Air Reduction, 60 E. 42nd St., New York 17, N. Y.

WEAR YOUR AWS EMBLEM

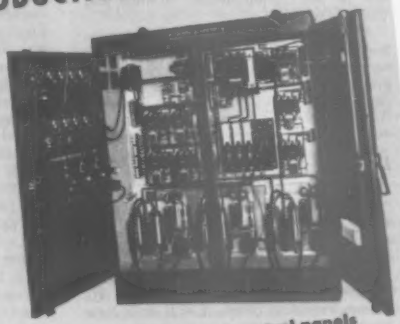
DESIGN LEADERSHIP

IN WELDER CONTROL FOR MASS PRODUCTION INDUSTRIES

A leading automobile manufacturer asked Square D to design and build magnetic and electronic control for a battery of 10 new multi-transformer welding presses. As many as 168 spot welds are made by one machine with 14 individually controlled timing periods in sequence.

Among the "must" requirements were:—welding current flow synchronized to prevent high starting transients and minimize stress on transformer windings; and a limitation of 1000 ampere current in each 3 phase line. Trouble-free performance on the production line has proven the successful solution by Square D engineers.

Engineering counsel on any electrical control problem is available through Square D field offices in all principal cities.



One of the completed control panels Because Square D's line of control is so complete, this entire unit was designed using only standard devices, accessories and electronic sub-assemblies.

SQUARE D COMPANY

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SECTION ACTIVITIES

prepared by C. M. O'Leary

Anthony Wayne

The regular monthly dinner meeting of the Anthony Wayne Section was held on January 19th in the Chamber of Commerce Bldg., Ft. Wayne, Ind.

Speaker was Lew Gilbert, Editor of *Industry and Welding*, whose subject "Maintenance Welding" was well received. Mr. Gilbert showed slides and gave a good talk of various plants not connected with welding doing all their own work, heavy repair and welding within their own plant. Everyone was interested in the whole program.

Arizona

Over 60 members and guests attended the December meeting of the Arizona Section which was held at the offices and plant of the Dye Oxygen Co. in Phoenix.

After a most enjoyable dinner served by the Dye Oxygen Co. an inspection trip was conducted through the plant by its Vice-President, Edward L. Thomas, who also gave a very thorough and enlightening talk on the "Manufacture, Storage and Uses of Oxygen."

All of those attending the meeting were impressed with the efficiency and magnitude of the plant operations and were very grateful to Gil Dye, President of the company for the fine program and dinner.

Boston

The regular monthly dinner meeting was held at M.I.T., Cambridge, on January 8th, with an attendance of 66 at dinner and 68 at the technical session.

Fortunately for the Boston Section, Munitions Board work brought H. W. Pierce, President of the A.W.S. into New England for a few days, so the Section was honored with his presence at dinner. After the dinner, Mr. Pierce gave the Section a very interesting outline of the major plan of the national organization for the SOCIETY'S welfare. The Boston Section thanks Mr. Pierce for taking time to attend this meeting.

Another speaker at the dinner was Ernest Tisko, Editorial Assistant in Foreign Affairs of the *Christian Science Monitor*. Mr. Tisko spoke on "The Meaning of Korea." His talk was very interesting especially since his travels in foreign countries gave him first-hand knowledge of the working of the Kremlin.

Speaker at the technical session was Harry Huff, Jr., Inert Arc Specialist of the Air Reduction Sales Co., whose subject, "Semiautomatic and Full Automatic Inert Tungsten Arc Welding," was highly interesting and instructive.

Bridgeport

January 18th dinner meeting of the Bridgeport Section was held at the Fairway Restaurant, Bridgeport, Conn. Speaker at the meeting was D. E. Boyd of Joseph T. Ryerson & Sons, who gave an extemporaneous talk on Stainless Steels and their applications.

Chattanooga

The December meeting of the Chattanooga section was held December 1st at the Maypole Restaurant. Dinner was at 6:30 P.M., preceded by a social hour. There were 23 members and guests present for dinner and several more attended the technical session.

E. B. Brown, the current chairman of the Detroit Section, was the speaker for the technical session. Graduated from the University of Michigan in 1928 with a Bachelor of Science degree in Electric Engineering, he spent the next three years with the Lincoln Electric Co. as a Sales and Service Engineer. From 1931 to 1935, Mr. Brown was employed by the U. S. L. Battery Corp., worked as a Safety Engineer and Electrical Inspector and was employed by the Plymouth Division of Chrysler Corp. In 1936 Mr. Brown joined the American Brass Co. as a Welding Engineer. He was advanced to the position of Development Engineer in 1942 and to Sales Engineer in 1949.

Mr. Brown has been very active in the AMERICAN WELDING SOCIETY. Since 1943, he has served as Chairman of the Reception and Program Committee, as Assistant Secretary, Secretary-Treasurer, Second and First Vice-Chairman, and is Chairman for the 1950-51 year.

Mr. Brown's major topic was the brazing and braze welding of metals using copper-base alloys. He used a number of excellent lantern slides to illustrate various applications. Mr. Brown also furnished a color film showing mining, smelting and refining of copper at the works of the Anaconda Mining Co. in Butte, Mont. His entire program was stimulating and well received as was evidenced by the questions and discussion from the audience after his lecture.

The January meeting was held on January 5th at Buquo's Restaurant. There were 32 people present for dinner, including an unusually large number of guests. George Tepley, Vice-Chairman, presided in the absence of W. L. Herbst, Chairman, who was out of town.

Main speaker during the technical session was John McClain, Industrial Engineer in charge of Incentives and Job Evaluation at Combustion Engineering-Superheater, Inc., Chattanooga Division. His topic was "Wage Incentives." Mr. McClain gave a thorough and lucid discussion of the basic features of the major wage incentive plans. He also made clear the relative advantages of each plan and the particular situations where certain plans would be preferable. He also discussed how base rates are determined and touched briefly on the results to be expected from wage incentive programs. Mr. McClain gave a clear picture of a varied and controversial subject to an interested audience, most of whom had been rather unfamiliar with this subject.

The February meeting of the Chattanooga Section was held Feb. 3, 1951, at Buquo's Restaurant. There were 27 members and guests present at dinner. The speaker for the Coffee Program was Jac Chambliss, a prominent local attorney. Mr. Chambliss spoke on the activities of the Citizen's Good Government League and on local and national politics.

M. C. Robbins, of Cleveland, Ohio, was the speaker for the technical session. Mr. Robbins is District Sales Manager of the Handy and Harman Co. His subject was Production Silver Alloy Brazing. Mr. Robbins traced the history and growth of silver alloy brazing and thoroughly discussed present and future application and methods. At the conclusion of his lecture Mr. Robbins presented a new color film showing the complete process of silver alloy brazing various products on actual production lines.

Cleveland

The fourth regular meeting of the season of the Cleveland Section was held January 10th at the Hotel Allerton. The attendance tally was well over one hundred with eighty at dinner. The dinner attendance included 13 guests.

Thirteen new members were formally introduced. Nine were present. Bill Steward, Cleveland Section Membership Chairman, and his Committee, are doing a good job signing up new members.

Herbert Hayward, the coffee speaker, told of the Ohio Boy's Village, a home for homeless boys. This is one of 71 similar institutions in the country privately financed and is the only one in Ohio. The

village took its first boys in September 1947, and at present is the home of 27 boys. They plan to accommodate 50 boys this year and to double each year until the ultimate capacity of 450 boys is reached.

The object of the village is the rehabilitation of boys 12 to 17 years old. Mr. Hayward remarked that several of the boys were interested in welding and they planned to provide equipment for the boys with this particular aptitude to use in maintenance work about the village and farm.

Technical speaker was C. C. Hand of the Stoody Co. Mr. Hand gave a very complete talk on factors and applications of hard facing. His talk was followed by almost an hour of audience participation of discussion and questions.

Mr. Hand gave examples of many types of businesses that make extensive use of hard facing and the dozens of ways in which hard facing is used. He also outlined some of the amazing results.

Steel mills have extended roll, shear and guide lift 10 to 100 times through the application of hard facing. Other applications are in the food industry in hammer mills and grinders. Hard facing makes possible the drilling through quartz rock and deep well drilling. Used on plow shares, 5 to 10 times as much acreage may be plowed. The use of hard facing on the farm has resulted in a farm market for many tons of rods annually.

Operations of many important industries are maintained at high levels through the use of hard facing.

The Cleveland Section's educational course in the metallurgy of welding got under way at Case Institute of Technology on February 6th. Thirty-four are taking the course under Prof. Jerry Cover. Two-hour classes will be held each Tuesday evening for eight weeks.

Colorado

January 9th dinner meeting of the Colorado Section was held in the Festival Room of the Oxford Hotel, Denver.

Harold F. Silver, President of the Silver Engineering Works, presented an excellent, extemporaneous paper on "The Art of Welding in Relation to Building Sugar Refining Equipment."

A 16-mm. film entitled "Master of Metals" was shown through the courtesy of The International Acetylene Assn.

Columbus

Howard S. Avery of the American Brake Shoe Co., Mahwah, N. J., was the guest speaker at the January 12th dinner meeting held in the Fort Hayes Hotel, Columbus, Ohio. Mr. Avery presented a clear-cut, interesting paper on "Hard Surfacing by Fusion Welding." This technique combines the advantages of protection in depth, with easy use of very wear-resistant alloys and hard compounds. Hard surfacing, the speaker pointed out, very often provides average savings of several hundred percent in material cost; occasional applications are much more fruitful. Indirect savings may also be high.

Cumberland Valley Division, Maryland Section

The January meeting of this Division developed into more than just an ordinary occasion. The meeting was a dinner affair which was well attended by members and guests; the guest speaker, M. J. Mianulli, Manager of the Customer Service Division, Titan Metal Mfg. Co., Bellefont, Pa., provided a pleasant personality and a lecture so interesting as to be classed surprising as it is not easy to make a technical subject fancy enough to listen to for over two hours, and that is what happened. Mr. Mianulli used plain, straight language and several pictures to carry his story from the melting pot to, and through, jobs which have been in service years after repair.

All in attendance were happy to have as visitors Mrs. Bernice M. McPherson, Secretary of the Washington Section, who told a bit about the Washington Section and the functions of the A.W.S.; Maynard E. McPherson filled the gap between dinner and lecture time with a talk about structural steel erection that would have been interesting to listen to for a much longer time. Also present was Mr. Layman, Washington representative of Titan Metal.

Honorable mention for attendance is due the officials and employees of the Frick Co., Waynesboro, Pa.

Dallas

Dinner meeting was held on January 10th in Brockles Restaurant, Dallas. Technical session followed in the Auditorium of the Lone Star Gas Co. Speaker at the technical session was George E. Linnert, Senior Research Engineer, Armco Steel Corp., whose subject was "Recent Developments in Welding Stainless and Heat-Resisting Steels." Mr. Linnert presented his subject in an exceptionally clear manner. All those present expressed their appreciation for the fine quality of the program. Refreshments were served after the meeting was adjourned.

Detroit

The April 13th meeting will be on the subjects: Design for Arc Welding and Design for Gas Welding. A Ladies Night Party will be held on May 11th.

Hartford

The January 11th dinner meeting was held at the Rockledge Country Club, West Hartford, Conn.

Harry A. Huff of the Air Reduction Sales Co. presented an extemporaneous talk on Inert-Arc Gas Shield with Consumable Electrodes. An excellent movie, "The Tool for the Job," was shown through the courtesy of the Air Reduction Sales Co.

Houston

Dinner meeting was held on January 11th at the Ben Milam Hotel, Houston, with an attendance of 90 at the dinner and 94 at the meeting.

George E. Linnert presented an excellent technical paper on "Recent Developments in Welding Stainless and Heat-Resisting Steels."

Before the meeting a movie, "The Tool for the Job," was shown.

Kansas City

The regular monthly dinner meeting of the Kansas City Section was held on January 18th at Fred Harvey's, Union Station.

Speaker at the technical session was J. E. Dato, Service Supervisor of the Southwestern Division of The Linde Air Products Co. Mr. Dato gave a very fine talk on "Argon Metal Arc Welding" which held the interest of everyone present. It was the best meeting of the year.

Lehigh Valley

E. C. Korten, Assistant Chief Engineer, Boiler Div., Hartford Steam Boiler Inspection & Insurance Co., was the guest speaker at the monthly meeting held on January 8th at the Hotel Bethlehem, Bethlehem, Pa. J. R. Fairhurst, Section Chairman, presided.

Mr. Korten gave a photoelastic method demonstration of stress distribution in welds. Plastic specimens simulating butt-welded joints were placed between Polaroid slides and projected upon a screen under tension, indicating the intensity of the stress under load. It proved to be a very interesting and informative demonstration, showing the highly stressed points in welds having an undercut, slag inclusions or incomplete penetration.

A dinner meeting was held at the Hotel Bethlehem prior to the technical session.

Los Angeles

The Los Angeles Section held their January meeting at Scully's Cafe on January 18th. The speaker was Francis Stevenson, a past-chairman of the Los Angeles Section and at present Development Engineer, Welding and Fabrication, Aerojet Engineering Corp., Azusa, Calif. The subject of Mr. Stevenson's talk was "Welding, Its Application to Jets and Rockets" and covered brazing techniques, resistance welding, fusion welding and welding of super alloys as related to jets and rockets. In addition to the talk a fifteen-minute color picture on the production and field testing of the Aerobee sounding rocket was presented.

An educational meeting of this Section was held on February 1st and consisted of a panel discussion on automatic welded overlays. Harold Sharp of C. F. Braun & Co. was the moderator and the speakers included Bob Hand of Stoody Co. and Fred Market of The Linde Air Products.

Louisville

LaMotte Grover, Welding Engineer for Air Reduction Sales Co. was guest speaker at the January 23rd meeting in the Rathskeller at Kapfhammer's. He gave a talk on "Use of Inert-Gas-Shielded Metal-Arc

Welding," which was well illustrated with lantern slides.

Mr. Grover explained that the higher quality and higher speeds of this process has opened up new fields where welding was not practical before. Equipment for both manual and automatic welding is available. Either argon or helium gas is used for the shielding, depending on the thickness of the material. This process can be used in all positions including overhead, Mr. Grover said, and can be used to join very thick material to very thin material.

Following the talk, a movie, "The Tool for the Job," was shown which very well illustrated the process and led to a discussion period. The evening closed with a social hour and a Dutch Lunch.

Milwaukee

The meeting dates for the remainder of the meetings of the Milwaukee Section are March 30th, April 27th and an Annual Party on May 26th.

New Jersey

A joint meeting with the Metropolitan Section, New Jersey Division of the A.S.M.E. was held on January 16th at the Essex House, Newark, N. J., with an attendance of 76 members and guests. A dinner in the Terrace Room preceded the meeting. The free dinner for the next meeting on February 20th was won by Larry Butler, Tidewater Oil Co., E. 22nd St., Bayonne, N. J.

Technical speaker was Fritz Albrecht, Welding Engineer of the Glenn L. Martin Aircraft Co., whose subject was "Comparison Between Welding and Other Methods of Fabrication in the Aircraft Industry." In Mr. Albrecht's talk, comparisons were made in the use of arc, spot, flash and cold welding with mechanical methods of fastening metals, used in the aircraft industry. The advantages of each method of joining were expertly described, as well as reasons for preference of each process used in fabricating present models of aircraft.

Frank C. Fyke, Technical Societies Representative, announced that the First Annual Conference of the Technical Societies Council will be held in the Mosque Theater, Newark, N. J., on Monday, Mar. 12, 1951. This conference is one in which all member societies will participate and will consist of a technical session, dealing with power, its source, and transmission. The subject matter and authors of the papers that will be presented are:

"Modern Synthetic Fuels," Dr. W. J. Sweeney, Vice-President, Standard Oil Development Co.

"Metal Problems in Combustion Engines," Dr. A. B. Kinzel, President, Union Carbide and Carbon Research Labs.

"Application of Fluid Drive in American Industry," J. W. Brennan, Manager, Hydraulics Coupling Div., American Blower Corp.

The principal dinner speaker will be George E. Stringfellow, Vice-President of Thomas A. Edison Co., Inc. Mr. Fyke urged all members to attend this conference.

Refreshments were served after the meeting.

New York

January 9th was the date of the New York Section's first meeting of 1951 when a symposium on Inert-Gas-Shielded Arc Welding was held. The two speakers were J. W. Mortimer, Welding Engineer, The Whitlock Mfg. Co., Hartford, Conn., and S. A. Yaczko, Welding Engineer. The M. W. Kellogg Co., Jersey City, N. J. The moderator of the meeting was E. H. Roper, Assistant Manager, General Technical Sales Dept., Air Reduction Sales Co., New York.

Mr. Mortimer spoke on the Inert-Gas-Shielded-Arc Welding Process using nonconsumable electrodes, while Mr. Yaczko covered the Inert-Gas-Shielded-Arc Welding Process using consumable electrodes.

Mr. Mortimer's paper discussed new developments in Inert-Gas-Shielded Arc Welding with tungsten electrodes. He noted the great improvements that have been made in recent years in the purity of the inert gases—argon and helium—which are used in the process and pointed out that this improvement in purity has overcome some of the early troubles in the welding of the more difficult to weld nonferrous alloys. He pointed out that the process has revolutionized aluminum welding providing faster and more economical welding of these metals than was possible with older methods. Mr. Mortimer covered the present status of the use of the process in welding copper, copper alloys, brass, silicon and aluminum bronzes. The new developments in equipment and the use of a.-c. transformers when using the Inert-Gas-Shielded-Tungsten-Arc Welding Process for welding aluminum were treated in Mr. Mortimer's paper. The very recent development of thoriated tungsten electrodes was also discussed.

Mr. Yaczko very competently discussed the Inert-Gas-Shielded-Arc Welding Process using consumable electrodes, which is the newer of the two processes. Using slides to illustrate his talk, Mr. Yaczko discussed the problems of welding refinery components with the process. The materials welded covered in the speaker's talk included the more difficult to handle from a welding standpoint, such as stainless steels and stainless clad materials. Mr. Yaczko indicated the proper sequence of operations required in welding the clad materials in order to prevent dilution of the cladding and to obtain a sound weld. He discussed the advantages of the process which make it one of the most versatile and economical of all the welding processes. Before closing Mr. Yaczko described the work his company has done in utilizing this process in overlay work using silicon bronze and aluminum bronze wires.

A new sound, color motion picture, "The Tool for the Job," produced by the Air Reduction Sales Co., a Division of Air Reduction Co., Inc., which depicted the development and outlined the fundamentals of the Aircomatic Process, was shown in conjunction with Mr. Yaczko's paper. A number of excellent sample welds made using the Inert-Gas-Shielded-Arc Welding Process with consumable electrodes were on display and evoked considerable interest.

Before the Technical Session, F. J. Socey, Jr., spoke as an after-dinner speaker. Mr. Socey is a free-lance weather forecaster and he gave his views of the weather in store for the next few months. Mr. Socey spoke before the New York Section last season and by popular demand was requested to speak again on this subject.

Both the dinner and technical session were held in Schwartz's Restaurant, 54 Broad St., New York. Meeting arrangements were made by S. T. Walter, Secretary-Treasurer of the New York Section.

Niagara Frontier

The March 22nd meeting will be held at the Hotel Sheraton, Buffalo, N. Y. Dinner will be served at 6:30 P.M. The meeting will start at 8 P.M. Speaker scheduled is F. A. Bodenheim, Sales Manager of Resistance Welding Division of Federal Machine & Welder Co. His subject, "Resistance Welding," will be a general talk supplemented by slides and movies.

A most interesting evening was enjoyed by all who attended the January 25th meeting of "Stump the Experts." There was an attendance of 118—the largest attendance of any meeting held during the past two years. It was a four-way contest between teams from Toronto, Rochester, Erie and Niagara Frontier to see which team gives the best answers to the questions submitted. It was a well-organized meeting with Charles H. Jennings in charge, and assisted by I. Morrison of the Morrison Steel Products and Clarence E. Jackson of the Union Carbide and Carbon Research Labs. Each team was called upon for answers eight times. The judges, four in all, made their decisions after each series and the points scored by each team were recorded on a large blackboard by Mr. Morrison. The final score was Niagara Frontier 62.5, Toronto 57.75, Rochester 51 and Erie (Northwestern Pa.) 57. The winner, Niagara Frontier.

Oklahoma City

Regular monthly meeting for January was held on the 9th in the Biltmore Hotel with George E. Linnert, senior research engineer of the Research Labs. of the Armco Steel Corp., Baltimore, Md., as the speaker. Mr. Linnert's talk was on recent developments in the welding of stainless and heat-resisting steel. His talk covered the recent and latest techniques and processes used for all types of stainless steels, also covered a very good discussion as to reason of failures in such type of steels. Much of the talk was devoted to coloring the different types of filler rods used in making welds in this type of steel that were not subject to hot cracking and porosity. The lecture was supplemented by a number of very good microscopic slides showing the effects of different type welding rods used, both correct and incorrect type, also the results of filler rods and fluxes having the low-melting alloys which

would deposit in the granular division line of such welded structure.

After the technical discussion of the meeting was over, a question-and-answer discussion followed, and some very important questions came up which were of additional benefit to quite a number of the members who are working in stainless steel products. After the discussion period, refreshments were served and a very good friendship conversation was held back and forth regarding the topics discussed. Everyone received the lecture very well and was well pleased with the way it was presented.

Pascagoula

The Pascagoula Section held its regular monthly dinner meeting on Wednesday, January 31st, at the Pascagoula Country Club. Twenty-seven men from varied industries surrounding Pascagoula attended the meeting. J. E. Durstine, Southern District Manager, and N. H. Dye, Welding Engineer of The Lincoln Electric Co., spoke on "Design for Lower Cost of Welding."

J. E. Durstine is an Electrical Engineer Graduate of Ohio State University. He has been with The Lincoln Electric Co. 26½ years, having served as Southern District Manager, with headquarters in Birmingham, Ala., since 1928.

N. H. Dye has been a Welding Engineer for the past four years with The Lincoln Electric Co. Mr. Dye attended Carnegie Tech and served with the Armed Forces in the South Pacific and Japan during World War II.

The paper was well illustrated with diagrams and sketches, which created enough interest among those present that the discussion period following lasted some forty-five minutes.

The March meeting is scheduled for the 28th. William Frederic Appleton, Structural Engineer, Gulfport, Miss., will speak on Welding Relative to Structural Design and Erection.

Philadelphia

The annual joint meeting with the Philadelphia Section of the Society of Naval Architects and Marine Engineers, for which the AMERICAN WELDING SOCIETY was host this year, was, as usual, one of the high lights of the year. The fine cooperation, fellowship and mutual interests between these two local sections make this meeting a success. The Meeting was held on Monday, January 15th.

"Welding and Fabricating High Temperature, High Pressure Piping" is a subject that is of particular interest to the Naval Architects and Marine Engineers at the present time, since they have just started to use the higher alloy materials for shipboard steam piping. It continues to be an interesting subject to members of the WELDING SOCIETY who are daily meeting more of these higher alloys in many types of applications requiring high temperature and high pressure.

L. C. McNutt, who is with the United Engineers and Constructors Inc., and in charge of welding at the new Sewaren Plant of Public Service of New Jersey, covered the welding and fabrication of the many alloy materials for high-temperature, high-pressure pipe in a thorough manner that only can be gained through experience in their use. He broke the materials into basic groups and gave the requirements which he has found necessary for satisfactory fabrication and welding.

Joint meeting Philadelphia Sections A.W.S. and Naval Architects. Left to right: H. W. Pierce, President, A.W.S. and meeting coordinator; L. C. McNutt, speaker; O. C. Frederick, Chairman of Philadelphia Section, A.W.S.; and J. A. Hill, Chairman of Philadelphia Section S.N.A. & M.E.

H. W. Pierce, Asst. to the President at New York Shipbuilding Corp., and our National President, acted as meeting coordinator to guide the discussion which was led off by prepared discussions by R. D. Bradway, Welding Engineer, New York Shipbuilding Corp., and H. J. Irrgang, Superintendent, W. K. Mitchell Co. A comparison was drawn between the general nature of shipwork which usually requires thin-wall, small-diameter pipe as compared to power plant work. The difficulty of carrying out some of the procedures under shipyard conditions was emphasized.

The general interest and discussion proved the importance of this subject and everyone's desire to keep up to date.

The panel discussion on Feb. 2, 1951, continued at the same high level that has come to be expected of these informal discussion meetings.

"Allowance for and Control of Shrinkage Due to Welding" gave the 52 members present a full evening with discussion halted only by the lateness of the hour. E. R. McClung, Superintendent, Lukenweld Division of Lukens Steel Co., acted as moderator and was assisted by the following Panel members: Arthur Holzbaur, Welding Engineer, Sun Shipbuilding and Dry Dock Co.; Ralph Bradway, Welding Engineer, New York Shipbuilding Corp.; Frank Kibblehouse, Welding Engineer, Welding Engineers Inc.; and Victor Nigriny, Supt. of Fabrication, McCarter Iron Wks. Shrinkage and distortion are always items that are open to discussion from which everyone can gain new ideas.

The meeting on Apr. 16, 1951, will be a double barreled attraction for the Philadelphia Section. The dinner will be followed by a Coffee Talk on our "1951 Phillies." The Technical session will consider a most important yet little understood subject, "Factors in Processing Steel Which Affect Weldability."

The final Panel discussion will be held April 6th as an open night with experts available to answer questions in any and all fields of welding. Plan to come and get an answer to your own problem.

Plans for the Dinner and Dance on May 12th are well under way, with the promise to surpass last year's successful event. Hold the date open and watch your meeting notice for details.

Portland

The biggest turnout of the season, totalling 32 persons, enjoyed an interesting dinner meeting held in the Lower Dining Room of the Elks Club on Nov. 14, 1950.

After the dinner hour the meeting was called to order by Charles J. Daniels, chairman.

A new sound movie concerning the forming, fabrication and erection of the new steel frame United Nations Bldg. in New York City was shown.

The principal speaker of the evening was then introduced by program chairman E. H. Weil. The speaker, Don H. Rasmussen, Western Sales Supervisor, Eutectic Welding Alloys Corp., delivered an excellent address on new applications and methods for using low-melting, nonfusion welding filler metals.

After a question-and-answer period on the subject, the meeting adjourned at 9:30 P.M.

Rochester

The January meeting of the Rochester Section was held on the 15th at the Century Sweet Shop. The dinner and meeting which followed drew an attendance of forty members.

Hallock C. Campbell, Associate Director of Research and Engineering of Arcos Corp., was guest speaker. His topic was "Recent Developments in Stainless Steel Welding." Most interesting was his explanation of the substitution of alloys to conserve scarce material during our national emergency. He also pointed out that the austenite-plus-ferrite structure of stainless steels had improved the physical properties without lessening the corrosive resistance to most agents. Mr. Campbell

concluded by answering a number of questions from the members. Rochester Section wishes to express its thanks to a very capable speaker.

In a "Battle of the Sections" at Buffalo, the Rochester Board of Experts trailed behind Niagara Frontier, Toronto and Northwestern Pennsylvania, but everyone enjoyed the contest.

In the membership drive, as of January 21st, Rochester is leading District 1, made up of nine Sections.

Susquehanna Valley

The regular monthly dinner meeting for February was held on the 7th at the Berwick Golf Club, Berwick, Pa.

Lewis Russell, Technical Chairman, discussed some of the technical activities of the SOCIETY.

Speaker at the technical session was G. O. Hoglund of the Aluminum Company of America. Mr. Hoglund's subject, "Welding Aluminum Alloys," covered new facts and methods and was presented in a clear-cut, interesting manner. A film entitled "Aluminum" was shown in conjunction with the talk.

Meetings scheduled for April and May are as follows:

April 4—J. S. Douglas, Engineer, The Linde Air Products Co. Subject: "Shielded Arc Welding."

May 2—Annual Meeting and Ladies Night.

Washington

The January dinner meeting was held on the 30th at O'Donnells Restaurant; the technical session at the Engineers Club. The guest speaker, W. H. Wooding, Superintendent of the Materials Branch, Industrial Testing Lab., Philadelphia Naval Shipyard, spoke on "Application of Low-Hydrogen Type Ferritic Electrodes." His lecture was illustrated with slides showing practical ways of fabricating armor plate and high-carbon steels in an economical manner. Mr. Wooding was assisted by C. A. Loomis in a Question Period.

LIST OF NEW MEMBERS

January 1 to January 31, 1951

ANTHONY WAYNE

Eager, Dorance (B)
Marquardt, Ron (B)
Noel, Lloyd L. (B)

ARIZONA

Weaver, Gene W. (C)

BOSTON

Fogg, Neal F. (B)
Gilbert, Leo A. (B)
Weener, George H. (C)

BRIDGEPORT

Lefkowitch, Leo (B)
Parmalee, Barnum E. (B)

CHICAGO

Folger, Allan B. (B)
Gilbert, Harold J. (B)
Kirby, William (C)
Meyer, Herman M. (B)
Thompson, George William (C)
Zale, Stanley F. (C)

CINCINNATI

Rasmussen, K. H. (C)

CLEVELAND

Allen, Richard I. (B)
Bannan, James J. (C)
Bartley, Harold (C)
Bigelow, Robert C. (C)
Brown, Irving C. (C)
Cox, John H. (B)
Dempsey, Thomas L. (C)
Fife, Alyn F. (C)
Foster, R. J. (B)
Fredette, Leo J. (C)
Gerstacker, William George (B)
Harvey, K. E. (B)
Heald, Sherman T. (C)
Jenkins, John C. (C)
Murdock, R. W. (C)
Noble, William, Jr. (C)
Olenir, Alex M. (C)
Saalwachter, Harold J. (C)
Sanders, Gene A. (C)
Smolka, Bernard (C)
Sproull, Theodore W. (B)
Wakefield, Willard W. (C)

COLUMBUS

Bubsey, Frank J. (C)
Cornell, Robert Ernest (D)
Cox, Frank H. (B)
Moore, Thomas J. (D)
Schlottig, Karl H. (C)
Willey, Robert (B)

DALLAS

Coulter, Charles D. (D)
Dillman, M. B. (D)
Glass, James K. (D)

DETROIT

Bingel, Dwight Charles (C)
Boenemar, Frank C. (C)
Frudd, Frank W. (B)
McGurn, Thomas D. (B)
Rippberger, Reginald R. (C)
Stephens, McDonald (B)
Swan, Lawrence A. (C)
Walen, Al (C)
Wilcoxson, R. B. (C)

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Cole, J. O. (B)
Davis, R. L. (C)
Smith, R. G. (C)
Surbaugh, P. G. (C)
Wells, Burton A. (C)

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Thompson, Robert N. (B)

KANSAS CITY

McCauley, Harry Allen (C)
Singleton, Frank G. (B)

LONG BEACH

Artese, Salvadore J. (C)

LOS ANGELES

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Richardson, Russell A., Jr. (C)
Shearman, C. R. (C)
Silva, Oliver Joe (D)
Storm, George (C)

MILWAUKEE

Heding, Wayland (C)
Schmeichel, N. L. (B)

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Mossman, R. W. (B)
Stottmeister, Walter F., Jr. (C)

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Buhr, Robert F. (C)
De Vito, Joseph (C)
Little, Robert S. (C)
Lynn, Milton M. (C)
Snider, Sidney R. (B)
Spinelli, Lorenzo (C)

NIAGARA FRONTIER

Whipple, Guy D. (B)

NORTHERN NEW YORK

Peters, Henry N. (C)

OKLAHOMA CITY

Barchus, Don (D)
Hutchison, Archie C. (D)
Morris, Kenneth E. (C)

PEORIA

Hess, Lewis S. (C)
Kelsey, H. C. (B)
Mills, Charles R. (C)
Stitt, Frank J. (C)
Tomlinson, Harry R. (C)

PHILADELPHIA

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Bintliff, J. Howard (B)
Brace, Arthur A. (C)
Liberatore, Edward C. (B)
Liese, Frank L. (B)
Lutz, William D. (B)
Nigriny, Victor (C)
Smith, Kenneth N. (B)
Solter, John B. (B)
Strenger, William T. (C)
Sultzer, William F., Jr. (B)
Van Artsdalen, Spencer (B)
Ward, John (B)

PORTLAND

Daniels, Charles J. (B)

PUGET SOUND

Downing, William H. (B)
Walker, William W. (B)

ROCHESTER

Gray, Sam (C)

SAGINAW VALLEY

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Perkins, Leonard J. (D)
Sodeberg, Earl W., Jr. (D)

SALT LAKE CITY

McClister, Claude (B)

SUSQUEHANNA VALLEY

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LeVan, Herman C. (C)

SYRACUSE

Jenkins, William (C)
Kagi, A. (C)

TULSA

Glasgow, Clarence (B)
O'Shields, William T. (B)

WESTERN MASSACHUSETTS

Randall, Norman H. (B)

WORCESTER

Abdow, Henry (B)
Earls, Martin (B)

NOT IN SECTIONS

Auer, Johannes Ulrich (B)
Finlay, J. M. (B)
Meinhart, Charles J. (D)
Taggart, Alfred (B)
Wyssbrod, Max M. (B)

Members Reclassified

During the month of January

BOSTON

Lynds, Clayton F. (C to B)

DETROIT

Hart, John H. (C to B)
Ratcliffe, Sidney (C to B)

PHILADELPHIA

Berg, L. D. T. (C to B)

abstracts of CURRENT WELDING PATENTS

prepared by V. L. Oldham

Printed copies of patents may be obtained for 25¢ from the Commissioner of Patents, Washington 10, D.C.

2,534,363—Blowpipe Apparatus—Edward Meincke, Scotch Plains, N. J., assignor to The Linde Air Products Co., a corporation of Ohio.

A blowpipe apparatus is disclosed and covered in this patent and it includes nozzle means having passage means therein for discharging gas and for discharging powder therefrom. One conduit connects to the passage means for supplying gas thereto and it has a valve provided therein whereas a second conduit communicates with the passage means and provides finely divided solid material thereto, whereas a mechanical conveyor is arranged for feeding the material from the second conduit into the passage means. An electric motor is operatively associated with the conveyor and a motor control switch is provided therefor, with such motor control switch being worked in unison with the valve means controlling supply of gas to the blowpipe.

2,534,365—Binozzle Gouging Torch and Method—Charles R. Moore, Mendham, and John Villoresi, Lincoln Park, N. J., assignors to The Linde Air Products Co., a corporation of Ohio.

This patent relates to a method of thermochemically gouging a groove in ferrous metal. The method comprises discharging two cutting oxygen streams at equal exit velocities of between 180 and 550 ft. per second with such streams lying in a common plane and forming an acute included angle of between 20 and 60°. Work-preheating, flame-forming jets of gas are discharged adjacent to each of the confluent streams of cutting oxygen. These streams are applied to the work and advance therealong for gouging a groove in such work.

2,534,526—Electrode Holder—David Murdock, Cincinnati, Ohio.

Murdock's patented holder includes a pair of resiliently movable conducting jaws for grasping an electrode, and conical roller cams, one of which is journaled in and fixed to each of the jaws. These cams project laterally from the jaws and a wedge is provided and is capable of being driven into relative rolling engagement with the cams so that movement of the wedge into and out of active engagement with the cams opens and closes same in unison.

2,534,593—Bonding Machine—Hans Hacklander, Linden, and Charles P. Sweeny, Somnerville, N. J., assignors to The Singer Mfg. Co., Elizabeth, N. J., a corporation of New Jersey.

This patent relates to an electrostatic bonding machine wherein a pair of electrodes are provided and have means associated therewith for establishing an electrostatic field of very high frequency therebetween.

2,534,643—Method for Brazing Beryllium—John Glenn Warner, Springdale, Conn., assignor to Machlett Lab. Inc., Springdale, Conn., a corporation of Connecticut.

Warner's patent relates to securing a member made substantially entirely of beryllium to a metallic base and includes the step of placing the base and the member against opposite faces of a thin layer of an alloy consisting of copper and an appreciable amount of beryllium not exceeding about 5.1%. The assembly so produced is placed between blocks of high resistance material and pressure is applied through the blocks. The space about the assembly and blocks is evacuated and current is passed through the assembly and blocks to melt the thin layer of copper alloy between the articles.

2,534,786—Welder's Inspection Light—Edward D. McElhaney and Frederick N. Michehl, Dunkirk, N. Y.

This patent covers a circuit connector for cables and relates specifically to a clamp particularly adapted for embracing the cable. This clamp has inner and outer concentric conductor rings and an intermediate insulation ring with a prong being carried by the inner ring contacting the cable upon clamping engagement of the clamp therewith.

2,535,187—Liquid Cooled Cable for Welding Equipment—Nelson E. Anderson, Scotch Plains, and Stephen L. Sullivan, Red Bank, N. J., assignors to Air Reduction Co., Inc., New York, N. Y., a corporation of New York.

This liquid-cooled cable includes a pair of flexible tubes twisted about each other longitudinally of the cable and a braid of conducting metal therearound. A casing of flexible material is disposed about and encloses the braid and provides a space for cooling water to flow therein.

2,535,873—Oxygen-Acetylene Torch—John E. Smith, Minneapolis, Minn., assignor to Smith Welding Equipment Corp., Minneapolis, Minn., a corporation of Minnesota.

Smith's patented torch discloses details of a handle which is adapted to be connected at one end to a supply of fuel gas and oxygen, whereas the other end of the handle is adapted to have a torch tip secured thereto. The handle is adapted to prevent leakage of fuel gases from the torch head.

2,535,946—Portable Manual Electric Spot Welder—Allan C. Mulder, Appleton, Wis., assignor to Miller Electric Mfg. Co., Appleton, Wis., a corporation of Wisconsin.

This patent is on a manually operated lightweight portable electric spot welder wherein a transformer is provided and has a core and end caps with a pair of relatively movable tongs being carried by one of the end caps and being connected therethrough to the secondary of the transformer. A carrying handle and an operating handle are provided and secured to one of the end caps. A mechanical power multiplier is disposed between the operating handle and the movable tongs so that the manual pressure applied by the operator is substantially increased between the ends of the tongs.

2,536,294—Arc Welding Apparatus—George G. Landis, South Euclid, and Norman J. Hoenie, Cleveland Heights, Ohio, assignors to The Lincoln Electric Co., Cleveland, Ohio, a corporation of Ohio.

This patent relates to an arc-welding system wherein a wire type of an electrode is fed in the direction of its length continuously to the work and an arc-welding current of high density is imposed on the electrode. Electrode feeding means are provided and an electric motor is provided for driving the electrode feeding means with the motor having its field connected to an independent fixed source of voltage and its armature connected across the welding current leads. Operation of the motor feeds the electrode independently of any traversing movement of the electrode relatively to the work. The flow of welding current through the leads is solely controlled by making and breaking contact between the arc-end of the electrode and the work.

2,536,726—Gas-Cooled Spot-Weld Tip—Leo W. Cornwall, San Diego, Calif.

This patent relates to a gas-cooled spot-weld tip having a one-piece tip member with a hollow portion at its inner end adapted to receive gas, and a circular in cross-section welding nib of solid construction at the opposite end of the tip. A plurality of passages is provided from the hollow portion adjacent

to the outer side of the welding nib, and the tip is provided with an annular groove communicating with the passages and surrounding the welding nib.

2,536,999—Apparatus for Electric Arc-Welding—Ernst Skytte, Copenhagen, Denmark, assignor to American Relay-Welder Co., Newark, N. J., a corporation of New Jersey.

This patent relates to an automatic electric arc-welding apparatus including at least one foot plate, at least one base rail extending longitudinally above the foot plate with at least two guide rails being slidably adjustable along the base rail and adjustably inclined at an acute angle to the plane of the foot plate. Each guide rail has a longitudinal slideway which has a slide disposed therein and carrying an electrode holder thereon. Means removably secure the foot plate to the work so that the bases of electrodes in the electrode holders will rest in the line of the weld and the vertical projections thereof will be substantially contiguous along the line of weld. Means are provided for automatically shifting the first electrode substantially at the end of its burning period in the direction of the following electrode until the arc of the first electrode is delivered to the following electrode.

2,537,385—Electric Welding Machine—Frank G. Wikstrom, Valley Stream, N. Y., assignor to Western Electric Co., Inc., New York, N. Y., a corporation of New York.

This patent relates to a welding machine which has means for feeding strip material to be welded and includes a pair of jaws for advancing the strip material in a straight line to the welding face of the electrode. A cutter is positioned adjacent the welding electrode and means are provided for moving the cutter in a straight line to cut a section from the strip material and carry the material transversely of the direction of the material feed to bring the severed sections into a position which is in alignment with and parallel to the face of the electrode.

2,537,776—Apparatus for Making Welded Pipe Joints—Arthur R. C. Markl, Louisville, Ky., assignor to Tube Turns, Inc., a corporation of Kentucky.

This patent is on a device for use in welding together the ends of tubular members and it includes an expansible ring that has a plurality of spaced outwardly extending studs intermediate the side edges thereof and around the periphery of the ring. Each of the studs is of greater length than the wall thickness of the tubular members to be connected and each stud has a weakened point at a distance from the ring less than the wall thickness of the tubular members. Thus the tubular members may be telescoped onto the ring and against the studs and be tack welded together after which the studs may be broken off at their weakened points within the space between the ends of the tubular members and the space filled with welding metal.

2,537,989—Pneumatic Welder—Harold J. Graham, Highland Park, Mich., assignor to Graham Mfg. Corp., Detroit, Mich., a corporation of Michigan

Graham's patent is on a welding gun for welding a stud to a workpiece. The gun includes a body having a cylindrical aperture therein with a main piston reciprocatingly disposed in the aperture and a chuck is adapted to hold a stud carried by the piston. An auxiliary piston is reciprocatingly disposed in the aperture for inserting the stud in the said chuck whereas a spring is disposed between the pistons. Means also are provided for admitting air under pressure to the end of the aperture adjacent the auxiliary piston to move same against the force of the spring so that it contacts the main piston to insert a stud in the chuck and subsequently moves the main piston until the stud contacts the workpiece to maintain a welding circuit including the power source for supplying energy to the point of contact. The stud is maintained in forcible contact with the workpiece until the welding is completed. Restoring means are present for returning the pistons after the completion of a weld

2,537,990—Electrical Welding Apparatus—Harold J. Graham, Highland Park, Mich., assignor to Graham Mfg. Corp., Detroit, Mich., a corporation of Michigan.

This particular apparatus includes electric energy supply means including energy storing means of the type re-establishing operative voltage after discharge. Means are provided for positioning two workpieces to be joined relatively to each other in place for welding but at a distance preventing direct contact therebetween. Impact means are included in the apparatus for bringing the pieces into force transmitting contact, and conductor means electrically connect the pieces to respective terminals of the supply means, whereas normally separated contacts are arranged in series connection in the conductor means for preparing. when closed, a welding circuit independently of the force-transmitting impact contact. Voltage responsive relay means are connected to the storing means for closing the contacts independently of and prior to the impact contact upon re-establishing of said operative voltage.

2,538,245—Welding Electrode Holder Switch—Gustave Hiller, Los Angeles, Calif., assignor to Edward R. McHugh, Los Angeles, Calif.

This patent particularly relates to an energizing control for welding rod holders and it includes a body of insulating material with a stationary contact thereon and an elongated conducting member comprising a pair of spaced spherical conductors connected by a rigid neck conducting portion. The insulating body has a socket to receive one of the spherical conductors to swingably mount the elongated conducting member for swinging movement of the other spherical conductor toward the stationary contact for circuit making purposes and away from same for circuit breaking purposes. A coil spring is recessed in the body and engages the other spherical conductor to urge same toward the stationary contact and being restrained from lateral movement at the engaging end thereof by its fit on the spherical surface of the other spherical conductor. Means are also provided for swinging the conducting member against the resistance of the coil spring, as are means for connecting an electric supply cable to the one spherical conductor.

Employment Service Bulletin

Positions Vacant

V-250. Junior Welding Engineer. Excellent opening for recent graduate welding or metallurgical engineer with some practical experience in welding. Position involves welding development, procedures and quality control. Large eastern fabricator of carbon steel and alloy materials. Please furnish details of experience, education, age and expected salary.

V-251. Wanted, a welding engineer on welded fabrication problems involved in machinery constructed such as boilers, turbines, piping, etc. Location: Middle Eastern District. Salary $6000.

V-252. Wanted an engineer capable of developing welding equipment and preparing specifications for welding equipment. Location: Middle Eastern District. Salary $5,000.

V-253. Assistant Welding Research Engineer. Recent graduate, metallurgical or electrical engineer with welding training. Excellent opportunity for advancement. Research and development in welding processes, design and application of mild steel, low-alloy steel, aluminum and stainless steel. Transition temperature studies. Applicant must be resourceful.

Services Available

A-612. Welding Engineer desires position. Consulting Engineer on construction and welding wishes to connect with a large shipyard. Has been a construction and welding engineer for the past 21 years on battleships, landing crafts, Naval aircrafts, Fleet Auxiliaries, ships and Coast Guard Vessels. Graduated from Penn State 1916.

A-613. Sales Engineer, familiar with all phases of gas and arc welding, desires position with manufacturer or distributor of gas- or arc-welding equipment and supplies. Twenty years' experience in servicing consumers, preparing advertising, establishing distributor and directing sales.

A-614. Welding Supervisor, experienced in all types of manual and automatic arc and gas welding on steel, nickel, copper and aluminum, desires a position with a progressive metal fabricator in a supervisory or engineering capacity.

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Supplement to The Welding Journal, March, 1951

Fatigue Tests of Beams in Flexure

Results of fatigue tests on various types of beams similar to those used in actual structures and subjected to repeated cycles of loads

DIGEST

THIS report embraces a description and a summary of the results of fatigue tests conducted by Prof. W. M. Wilson of the University of Illinois on various types of beams similar to those used in actual structures and subjected to repeated cycles of loads.

The tests consisted essentially of subjecting various types of beams to repeated loads varying from a small load on the beam to the maximum in the cycle. This load cycle was repeated at about 150 cycles per minute until failure developed at some location in the beam. The stresses in the beams were calculated from the known loads, and the fatigue strengths at 100,000 cycles and 2,000,000 cycles of load were determined from an empirical equation.

Fatigue data were obtained on 27 different types of beams and a total of 104 specimens were tested. A description of each series or type, with the average fatigue strength obtained for each series at 100,000 cycles and 2,000,000 cycles, is shown in Table 1, and a comparison of the carrying capacities of typical types is shown in Table 2.

The results of these tests are of particular interest to the bridge engineer constructing and maintaining stringers and beams subjected to a large number of cycles of stress, for they indicate the following outstanding features:

1. The carrying capacity of a rolled beam is considerably higher than that obtained with a fabricated beam of equivalent section modulus.

2. The carrying capacity of a beam with cover plates cannot be increased indefinitely with an increase in cover-plate thickness, as there is some economic relation between the cover-plate area and the flange area of the beam.

3. The use of cover plates on rolled beams appears to be limited to reinforcement jobs where the beam is already in place. In such cases an increase in carrying capacity can be obtained, but this increase in capacity will not increase as rapidly as the calculated section modulus.

4. Cover plates on beams should be full length or extended past the theoretical cut-off point a sufficient distance, so that the stress in the beam at the end of the plate is only about 40% of the stress at the center.

5. The stress-raising effect of welding across the ends of partial length cover plates is about the same as that of the longitudinal welds along the edges of the cover plates.

6. The stress-raising effect of continuous fillet welds when used to attach cover plates to beams is smaller than that of intermittent fillet welds or rivets.

7. The carrying capacity of a beam is always reduced whenever intermediate stiffeners or lateral plates are welded to the tension flanges.

8. It is impossible to splice a beam by butt welding or riveting, and attain a load carrying capacity equal to that of a continuous rolled beam of equal section modulus.

9. It is doubtful if fatigue failures will occur in the longer girder spans, as the fatigue strength at 100,000 cycles of stress range for the various types of specimens is fairly high.

FOREWORD

The fatigue tests of beams in flexure summarized in

this report were conducted at the University of Illinois under the direction of Professor W. M. Wilson, research professor of structural engineering, and the details of all the test results have been published in the University of Illinois Engineering Experiment Station Bulletins 377 and 382.

The tests were under the general supervision of the Committee on Fatigue Testing (structural) of the Welding Research Council. The tests were planned by a subcommittee on Flexural Fatigue Tests, with G. M. Magee, research engineer, A.A.R. as chairman. Several members of A.R.E.A. Committee 15—Iron and Steel Structures—were members of the subcommittee. This summary report was prepared by E. J. Ruble, structural engineer, research staff, A.A.R.

The principal purpose of the investigation was to determine the relative fatigue strength of various types of bridge members subjected to flexure. It was realized by bridge engineers that the stringers and beam spans of some bridges are subjected to a considerable number of cycles of stress, varying from a very small dead load stress to a maximum stress. It was realized also, that these stringers and beam spans usually have geometrical stress-raisers, such as partial length cover plate, lateral plates welded or riveted to the flanges, intermediate stiffeners welded to the flanges and web, and welded or riveted full length cover plates, and that very little information was available on the effect of these stress-raisers.

The actual number of stress cycles and the range of the stresses occurring in any bridge are usually variable quantities, except in crane girders where the magnitude and number of the loads are fairly well known. Railroad and highway bridges are usually subjected to loads of various static weights and, in addition, the dynamic effects are not always the same, so that the magnitude of the stress cycle cannot be accurately determined.

It has been generally agreed in the past that in short-span bridges, where the stresses produced by heavy car and tender axles are about the same as those produced by the locomotive axles, the number of stresses cycles attained could reach about 2,000,000. In the longer span bridges, where the maximum stresses are produced under the locomotive and tender together, the number of stress repetitions could reach 100,000 during the life of the structure. During the past few years, the research staff of the A.A.R. has secured strain gage readings on railroad bridges of all lengths under actual operating conditions, and a considerable range in the actual stresses has been found with only a small percentage of the loads producing maximum stresses in the structures. Considerably more field work must be done before the frequency of occurrence of the maximum stresses can be determined, as well as laboratory work to determine the effect on the fatigue strength of various stress ranges on the same specimen.

METHOD OF TESTING

The structural beams were tested in flexure under cycles of stress varying from a small tensile stress of about 500 psi., to the maximum tensile stress by means of the fatigue machine shown in Fig. 1. The force that produced the bending stress in the beams originated in the variable-throw eccentric that raised and lowered the loading lever, thereby subjecting the loading column to cycles of load. The magnitude of the force at the

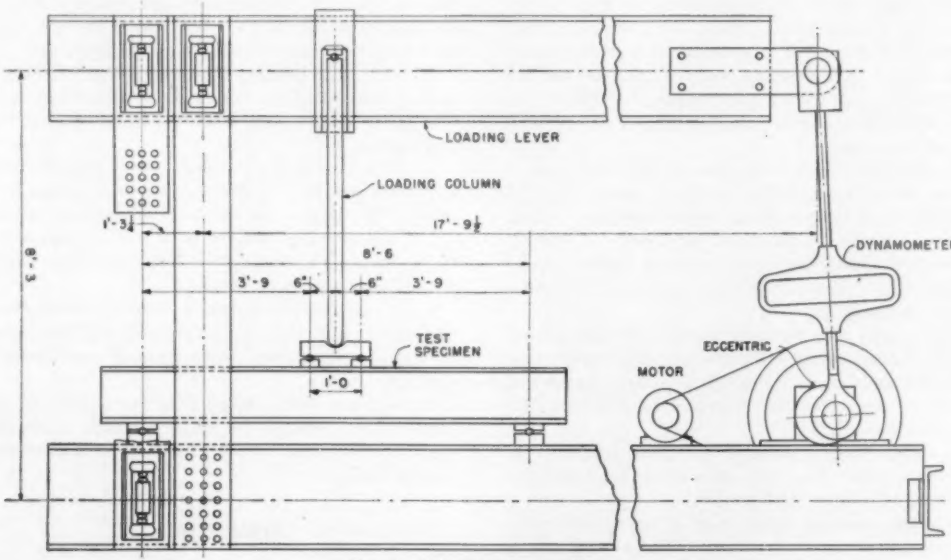

Fig. 1 Fatigue-testing machine

end of the loading lever was measured by the calibrated dynamometer or proving ring.

In operation, the specimens were placed in the machine and supported on rollers resting in cylindrical grooves. The grooves were somewhat larger in diameter than the rollers, to prevent the rollers from introducing a horizontal restraint. The machine was then cranked by hand while the eccentric was being adjusted to give the desired load. The machine was then started and run at a speed of approximately 150 cycles of load per minute, but was stopped from time to time for load adjustment as required. The stresses in the beams were

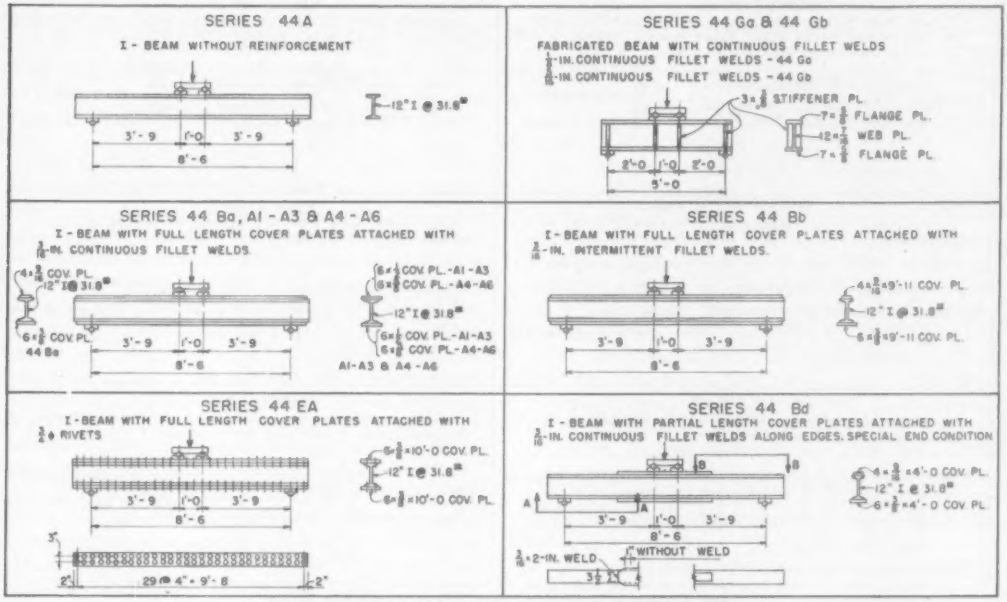
SERIES 44 A
I - BEAM WITHOUT REINFORCEMENT
SERIES 44 Ga & 44 Gb
FABRICATED BEAM WITH CONTINUOUS FILLET WELDS
SERIES 44 Ba, A1 - A3 & A4 - A6
I - BEAM WITH FULL LENGTH COVER PLATES ATTACHED WITH
IN. CONTINUOUS FILLET WELDS
SERIES 44 Bb
I - BEAM WITH FULL LENGTH COVER PLATES ATTACHED WITH
IN. INTERMITTENT FILLET WELDS.
SERIES 44 EA
I - BEAM WITH FULL LENGTH COVER PLATES ATTACHED WITH
RIVETS
SERIES 44 Bd
I - BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH
IN. CONTINUOUS FILLET WELDS ALONG EDGES. SPECIAL END CONDITION

Fig. 2 Details of various types of beams tested in flexure

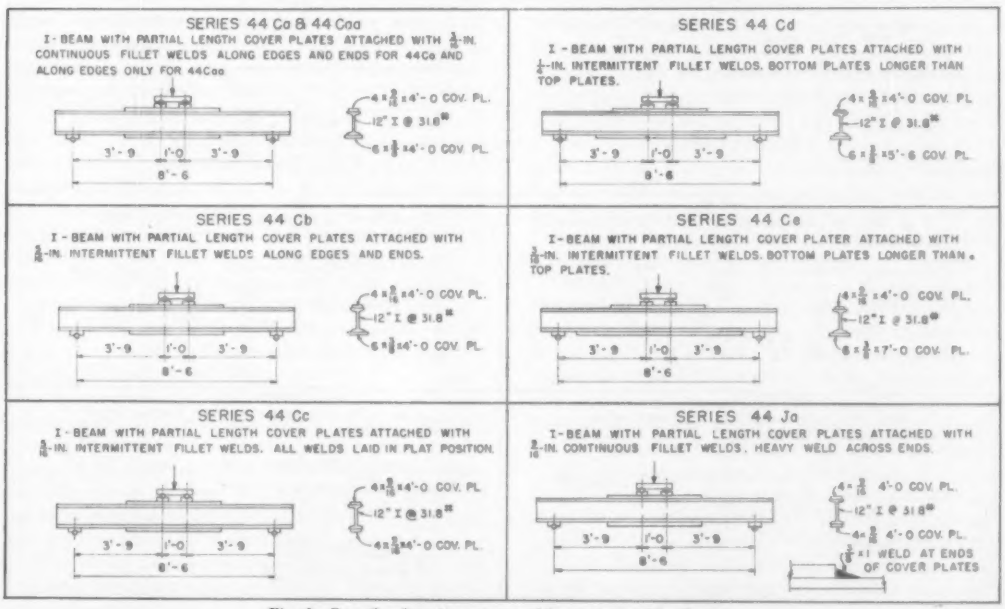
SERIES 44 Ca & 44 Caa
I - BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH IN. CONTINUOUS FILLET WELDS ALONG EDGES AND ENDS FOR 44Ca AND ALONG EDGES ONLY FOR 44Caa
SERIES 44 Cd
I - BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH IN. INTERMITTENT FILLET WELDS. BOTTOM PLATES LONGER THAN TOP PLATES.
SERIES 44 Cb
I - BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH IN. INTERMITTENT FILLET WELDS ALONG EDGES AND ENDS.
SERIES 44 Ce
I - BEAM WITH PARTIAL LENGTH COVER PLATER ATTACHED WITH IN. INTERMITTENT FILLET WELDS. BOTTOM PLATES LONGER THAN TOP PLATES.
SERIES 44 Cc
I - BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH IN. INTERMITTENT FILLET WELDS. ALL WELDS LAID IN FLAT POSITION.
SERIES 44 Ja
I - BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH IN. CONTINUOUS FILLET WELDS. HEAVY WELD ACROSS ENDS.

Fig. 3 Details of various types of beams tested in flexure

not measured but were computed by the usual flexural formula from the load in the loading column as indicated by the dynamometer.

A static test was made on one specimen each of several types of beams, which was identical with the fatigue specimen of the corresponding series. The specimen was loaded and supported in the same manner for the static tests as for the fatigue tests. The compression flange had no lateral support except that afforded by the loading head of the testing machine.

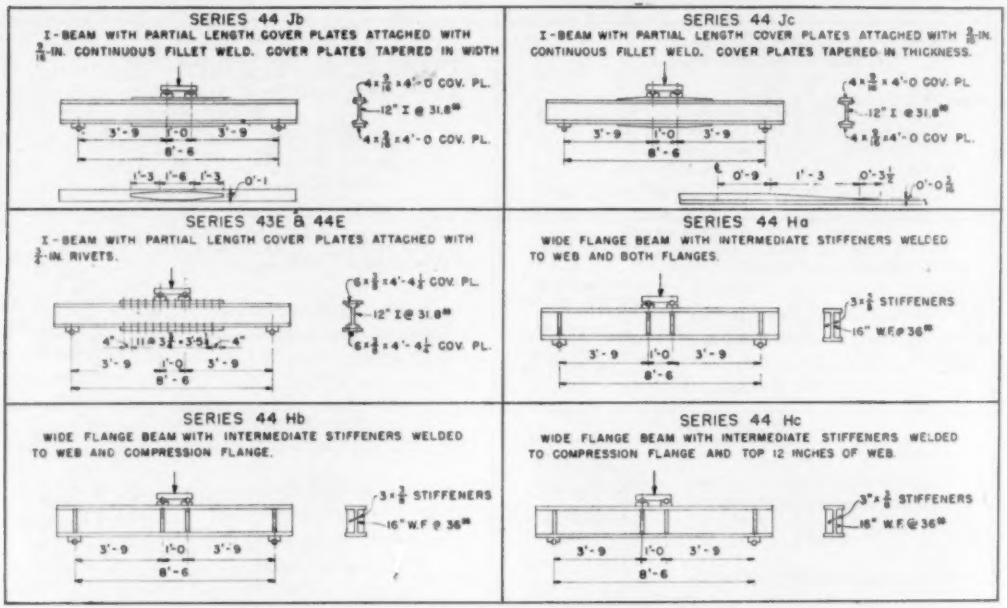
SERIES 44 Jb
I-BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH 5/16-IN. CONTINUOUS FILLET WELD. COVER PLATES TAPERED IN WIDTH
SERIES 44 Jc
I-BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH 5/16-IN. CONTINUOUS FILLET WELD. COVER PLATES TAPERED IN THICKNESS.
SERIES 43E & 44E
I-BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH 3/4-IN. RIVETS.
SERIES 44 Ha
WIDE FLANGE BEAM WITH INTERMEDIATE STIFFENERS WELDED TO WEB AND BOTH FLANGES.
SERIES 44 Hb
WIDE FLANGE BEAM WITH INTERMEDIATE STIFFENERS WELDED TO WEB AND COMPRESSION FLANGE.
SERIES 44 Hc
WIDE FLANGE BEAM WITH INTERMEDIATE STIFFENERS WELDED TO COMPRESSION FLANGE AND TOP 12 INCHES OF WEB.

Fig. 4 Details of various types of beams tested in flexure

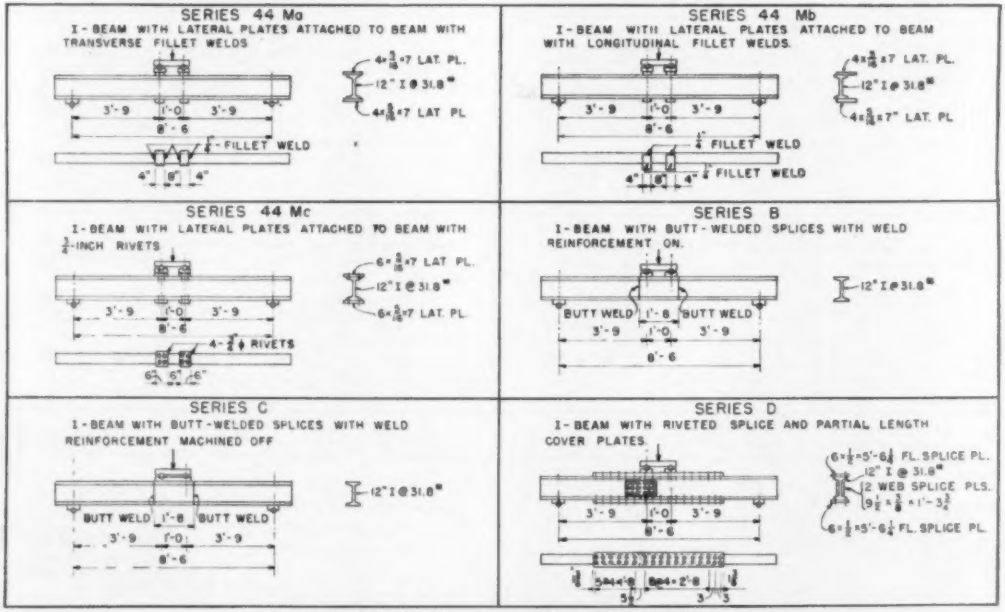
SERIES 44 Ma
I-BEAM WITH LATERAL PLATES ATTACHED TO BEAM WITH TRANSVERSE FILLET WELDS
SERIES 44 Mb
I-BEAM WITH LATERAL PLATES ATTACHED TO BEAM WITH LONGITUDINAL FILLET WELDS.
SERIES 44 Mc
I-BEAM WITH LATERAL PLATES ATTACHED TO BEAM WITH 3/4-INCH RIVETS
SERIES B
I-BEAM WITH BUTT-WELDED SPLICES WITH WELD REINFORCEMENT ON.
SERIES C
I-BEAM WITH BUTT-WELDED SPLICES WITH WELD REINFORCEMENT MACHINED OFF
SERIES D
I-BEAM WITH RIVETED SPLICE AND PARTIAL LENGTH COVER PLATES.

Fig. 5 Details of various types of beams tested in flexure

TEST SPECIMENS

The general details of the 27 different types of beams tested are shown in Figs. 2 to 5, incl., and a brief description of each series follows

The specimens in Series 44A consisted of a 12-in. I at 31.8 lb. without reinforcement, and the results of the tests on these specimens form the base for comparing the results of the tests on the remaining types. A total load of 16,000 lb. on the specimen at the center of the span was required to produce a calculated unit stress of 10,000 psi. in the flanges under the loading pins.

The specimens in Series 44Ba, A1–A3 and A4-A6 consisted of three slightly different types, the principal difference being in the thickness of the full length cover plates. The three different types were selected to determine whether the carrying capacity of a beam with cover plates subjected to repeated cycles of load increased directly with the increased section modulus. The beams in Series 44Ba had a 6 by $^{3}/_{8}$-in. cover plate on the lower flange, with a 4 by $^{9}/_{16}$-in. cover plate on the upper flange. The different width cover plates were used to simulate field practice whereby overhead welding is eliminated. The beams of Series A1–A3 had two 6 by $^{1}/_{2}$-in. cover plates while two 6 by $^{5}/_{8}$-in. cover plates were used to fabricate the beams of Series A4–A6. The use of the same width cover plates on the upper and lower flanges required a different welding technique than that used in Series 44Ba, but since all failures occurred in the lower flange, the results were considered comparable for all three series.

A total load of 34,500 lb. was required on the specimen of Series A4–A6 at the center of the span to produce a calculated unit stress of 10,000 psi. in the 6 by $^{5}/_{8}$-in. cover plates under the loading pins, with a resulting calculated unit stress of 2980 psi. in the $^{3}/_{16}$-in. continuous fillet welds.

The specimens of Series 44EA consisted of a 12-in. I at 31.8 lb. beam reinforced with two 6 by $^{3}/_{8}$-in. cover plates attached with $^{3}/_{4}$-in. round rivets. The section modulus of the gross section was 61.0 in.^{3} while the section modulus of the net section was only 45.3 in.^{3} resulting in an efficiency of 74.3%. The tests on the specimens of this series were made so that the results could be compared with those secured on specimens having cover plates fastened with welds.

Series 44Ga and Series 44Gb were identical, except for the size of the continuous fillet welds fastening the 7 by $^{5}/_{8}$-in. flange plates to the 12 by $^{7}/_{16}$-in. web plate. The beams were reinforced with 3 by $^{3}/_{8}$-in. stiffener plates welded to the web plate only. The end stiffeners were welded to the web plate their full length, but the intermediate stiffeners were welded only to the compression half of the web. The corners of the stiffeners were clipped so that the fillet welds fastening the flange plates to the web plate could be continuous.

In an effort to produce fatigue failures in the welds fastening the flange plates to the web, the span length for this series was reduced to 5 ft. However, the plan was not effective as no failures occurred in these welds. The flange plates of Series 44Ga were welded to the web plate by continuous fillet welds and then reduced to $^{1}/_{8}$-in. welds by machining off the surplus. The specimens of Series 44Gb were fabricated with $^{3}/_{16}$-in. continuous fillet welds.

A unit stress of 9850 psi. was calculated in the $^{1}/_{8}$-in. fillet welds from a total load of 51,900 lb. on the specimen at the center, which also produced a unit flange stress of 10,000 psi. under the loading pins. This same total load on the specimens of Series 44Gb produced a calculated unit stress of 6580 psi. in the $^{3}/_{16}$-in. fillet welds.

The full length cover plates on the specimens of Series 44Bb were fastened to the beams with $^{3}/_{16}$-in. intermittent fillet welds. The welds were $2^{1}/_{4}$ in. long and were spaced at 6-in. centers. A total load of 26,600 lb. on the specimen at the center of the span produced a calculated unit stress of 10,000 psi. in the cover plates under the loading pins and a calculated stress in the $^{3}/_{16}$ by $2^{1}/_{4}$-in. fillet welds of 4810 psi.

The specimens of Series 44Bd consisted of I-beams reinforced with partial length cover plates. The cover plates were fastened to the beams with $^{3}/_{16}$-in. continuous fillet welds along their edges, with special weld conditions at the ends of the bottom cover plate. The cover plates were cut off at the theoretical points so that the stress in the beam flanges at these points was about the same as the stress in the cover plates between the loading pins. The cover plates were cut off at this point so that the full stress-raising effect could be determined.

The $^{3}/_{16}$-in. fillet welds fastening the 4 by $^{9}/_{16}$-in. top cover plate to the beam were run continuous around the ends of the plate. The corners of the bottom cover plate were clipped to produce a $3^{1}/_{2}$-in. width plate and then $^{3}/_{16}$ by 2-in. welds were placed along the ends of the plate. The $^{3}/_{16}$-in. continuous welds along the plate edges were stopped 1 in. from the clipped corners.

The partial length cover plates reinforcing the specimens of Series 44Ca were fastened to the I-beams with $^{3}/_{16}$-in. continuous fillet welds along the edges, around the corners and across the ends of the plates. The top cover plates were 4 by $^{9}/_{16}$ in., while 6 by $^{1}/_{2}$-in. plates were used on the bottom flange.

The specimens of Series 44Caa were identical to those of Series 44Ca, except for the welds across the ends of the cover plates. The continuous welds on the sides of the specimens of Series 44Caa were stopped at the corners and did not extend across the beams at the ends of the cover plates. The specimens of this series were fabricated and tested to determine the effect of welding transverse to the line of stress in a member.

The partial length cover plates reinforcing the specimens of Series 44Cb consisted of a 4 by $^{9}/_{16}$-in. top plate and a 6 by $^{3}/_{8}$-in. bottom plate. The cover plates were attached to the beams with $^{5}/_{16}$-in. intermittent fillet welds on the sides, the welds each being $2^{3}/_{4}$ in. long, spaced at 6-in. centers, and with $^{5}/_{16}$-in. continuous fillet welds across the ends of the cover plates. The

calculated stress in the beam flanges at the ends of the plates was about the same as that in the cover plates between the loading pins.

The partial length cover plates reinforcing the specimens of Series 44Cc were fastened to the I-beams with 5/16-in. intermittent fillet welds along the edges and 5/16-in. continuous fillet welds across the ends of the cover plates. Both top and bottom cover plates were 4 by 9/16 in. and all the welds were laid in the flat position by turning over the specimens during welding. The intermittent fillet welds along the edges of the cover plates were 2 3/4 in. long and were spaced at 6-in. centers. The calculated stress in the beam flanges at the ends of the plates was the same as that in the cover plates between the loading pins.

The specimens of Series 44Cd consisted of I-beams reinforced with a partial length cover plate on the top flange and a longer plate on the lower flange. The cover plates were attached to the beams with 1/4-in. intermittent fillet welds on the sides, the welds being 2 3/4 in. long, spaced at 6-in. centers, and with 1/4-in. continuous fillet welds across the ends of the cover plates.

In order to produce failure in the top flange, the cover plate on the lower flange was extended past the theoretical cut-off point so that the stress in the beam flange at the end of the plate was only 67.5% of the stress in the cover plate between the loading pins. The calculated compressive stress in the top flange of the beam at the end of the cover plate was 92.3% of the stress in the cover plate under the loading pins.

The specimens of Series 44Ce consisted of I-beams reinforced with a partial length cover plate on the top flange and a much longer plate on the lower flange. The lower flange cover plate on these specimens was extended past the theoretical cut-off point until the stress in the beam flange at the end of the plate was only 34% of the stress in the cover plate between the loading pins. The longer cover plate was used on the specimens of this series, as one specimen of Series 44Cd failed at the end of the lower cover plate, even though the stress at this point was only 73.3% of that at the end of the top plate.

The cover plates were attached to the beams with 5/16-in. intermittent fillet welds—the welds being 2 3/4 in. long, spaced at 6-in. centers—and with 5/16-in. continuous welds across the ends of the cover plates.

The partial length cover plates reinforcing the specimens of Series 44Ja were fastened to the I-beams with 9/16 by 1/2-in. continuous fillet welds on the edges of the plates and with heavy welds across the ends. Both the top and bottom cover plates were 4 by 9/16 in., and all the welds were laid in the flat position by turning over the specimens during welding. The partial length cover plates resulted in the calculated stress at the ends of the plates being about the same as the stress in the plates between the loading pins.

The continuous welds along the edges of the cover plates were 9/16 in. high but the 5-in. flange width of the beam only permitted the welds to be 1/2 in. wide. Three of the specimens were fabricated with 3/8 by 3/4-in. welds at the ends of the cover plates and three were fabricated with 3/8 by 1-in. welds at the ends.

The specimens of Series 44Jb consisted of I-beams reinforced with partial length tapered cover plates. The cover plates were fastened to the beams with 9/16-in. continuous fillet welds along the edges and ends of the plate. The welds along the full width of plate were only 1/2 in. wide as permitted by the 5-in. beam flange width.

The top and bottom covered plates were tapered in width only from 4 to 1 in. and the calculated stress in the beam flange at the end of the cover plate was the same as that in the cover plates between the loading pins. The tapered cover plates were used for this series in an effort to reduce the abrupt change in section at the ends of the plates.

The partial length cover plates reinforcing the specimens of Series 44Jc were reduced in thickness from 9/16 to 5/16 in. in an effort to eliminate the abrupt change of section at the ends of the plates. The 4-in. width of plate was maintained for its entire length.

Both top and bottom cover plates were fastened to the beam with 9/16-in. continuous fillet welds along the edges of the plates for their center portion, and the vertical legs of the welds were then reduced along the tapered edges of the plate. The welds on the ends of the plates were tapered by grinding from 5/16 in. down to a feather edge as shown.

The stress in the beam at the end of the 3 1/2-in. welds was only about 87% of the stress in the cover plates between the loading pins.

The partial length 6- by 3/8-in. cover plates reinforcing the specimens of series 43E and 44E were fastened to the I-beams with 3/4-in. diameter rivets. The section modulus of the gross section of the reinforced member was 61.0 in.,[3] while the net section modulus was 48.9 in.[3] The net section modulus of the beam at the end of the cover plate was 26.9 in.[3] The tests on the specimens of this series were conducted so that the results could be compared with those secured on specimens having partial length cover plates fastened with welds.

The specimens of series 44Ha, 44Hb and 44Hc consisted of a 16 WF beam at 36 lb., with the web reinforced with 3- by 3/8-in. stiffeners on both sides of the web at the points of loading and bearing. The corners of the stiffeners were clipped to clear the fillets of the beams.

The intermediate stiffeners under the loading pins for Series 44Ha were welded to both the tension and compression flanges, and to the full depth of the web, with continuous fillet welds. The intermediate stiffeners of Series 44Hb were welded to the compression flange only, and to the full depth of the web, with continuous fillet welds. The intermediate stiffeners of Series 44Hc were welded to the compression flange only, and to the top 12 in. of the web, with continuous fillet welds.

The end stiffeners of all three series were ground to fit the lower flange but cleared the top flange. The stiff-

eners were welded to the web only with continuous fillet welds.

The specimens of series 44Ma, 44Mb and 44Mc consisted of I-beams with small plates attached to the top and bottom flanges under the loading pins. The specimens of these three series were representative of actual practice, whereby lateral plates and angles are attached to the flanges of stringers.

The 4- by $^5/_{16}$- by 7-in. plates in Series 44Ma were placed symmetrically on the beam flanges and attached to the beam with $^1/_4$-in. continuous fillet welds transverse to the beam. The 4 by $^5/_{16}$- by 7-in. plates in Series 44Mb were placed unsymmetrically on the beam flange so that the plates could be attached to the beam with $^1/_4$-in. continuous fillet welds longitudinal to the beam. The 6 by $^5/_{16}$- by 7-in. plates in Series 44Mc were placed symmetrically on the beam flange and were fastened to the flange with 4 $^3/_4$-in. diameter rivets.

Each specimen of series B and C was fabricated from three short-beam sections butt-welded together. The tests were planned to simulate conditions encountered in long continuous beam spans where it becomes necessary to splice the beams. The ends of the beam flanges and webs were chamfered and for all specimens, except one, the web was welded first and then the flanges. A series of tiedown clamps was used to hold the three short lengths of beams forming a specimen in line during welding.

The specimens of series B and C were identical, except that the specimens of Series B were tested in the as-welded condition, while the weld reinforcement on the specimens of Series C was ground flush with the base metal.

The specimens of Series D were fabricated from two beam sections with a riveted splice, and were planned so that the results could be compared with the results obtained on the butt-welded specimens. The specimens were designed for fatigue failure on a section through the inside row of rivets in both the cover plates and the web-splice plates. To make the specimens fail at this section, the cover plates were extended far enough so that failure would not occur at the ends of the plates.

The gross section modulus of the plain beam was 36 in.^{3}, while the gross section modulus of the splice plates was 44.4 in.^{3} The net section, modulus of the beam was 26.9 in.^{3}, compared with a net section modulus of 33.4 in.^{3} for the splice plates.

RESULTS OF TESTS

A summary of the results obtained from the static and fatigue tests of the various types of beams in flexure is shown in Table 1. The values shown in this table are the results of one static test on a specimen of each series, and the average of several results for the fatigue tests of each series.

The yield point and ultimate static strength values shown in Table 1 were calculated from the maximum load which the specimen would carry, applying the flexural equations used in engineering design. However, it must be kept in mind that the values shown are not

Table 1—Static and Fatigue Strength of Various Types of Beams in Flexure

TEST SERIES	DESCRIPTION OF TEST SPECIMEN	STATIC STRENGTH KIPS PER SQ. IN.		AVERAGE FATIGUE STRENGTH KIPS PER SQ. IN.	
		YIELD POINT	ULTIMATE	AT 100,000 CYCLES	AT 2,000,000 CYCLES
44A	I-BEAM WITHOUT REINFORCEMENT	35.9	43.8		31.2
44Ga	FABRICATED BEAM WITH $\frac{1}{4}$-IN. CONTINUOUS FILLET WELDS		55.0	49.8	17.2
44Gb	FABRICATED BEAM WITH $\frac{1}{4}$-IN. CONTINUOUS FILLET WELDS		54.8	45.2	16.6
44Ba	I-BEAM WITH FULL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{16}$-IN. CONTINUOUS FILLET WELDS ($\frac{5}{8}$-IN. BOTTOM PLATE)	42.2	59.3	41.1	22.8
A1-A3	I-BEAM WITH FULL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{16}$-IN. CONTINUOUS FILLET WELDS. ($\frac{1}{2}$-IN. BOTTOM PLATE)				23.4
A4-A6	I-BEAM WITH FULL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{16}$-IN. CONTINUOUS FILLET WELDS. ($\frac{3}{8}$-IN. BOTTOM PLATE)				20.3
44Bb	I-BEAM WITH FULL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{16}$-IN. INTERMITTENT FILLET WELDS.	37.5	49.2	42.4	16.5
44EA	I-BEAM WITH FULL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{4}$-IN. DIAMETER RIVETS.				15.8 ▲
44Bd	I-BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{16}$-IN. CONTINUOUS FILLET WELDS ALONG EDGES. SPECIAL END CONDITIONS			22.1	10.0
44Ca	I-BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{16}$-IN. CONTINUOUS FILLET WELDS ALONG EDGES AND ENDS.	37.5	45.8	19.4	9.3
44Caa	I-BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{16}$-IN. CONTINUOUS FILLET WELDS ALONG EDGES.				8.9
44Cb	I-BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{16}$-IN INTERMITTENT FILLET WELDS ALONG ENDS AND EDGES		39.3	19.0	8.4
44Cc	I-BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{16}$-IN. INTERMITTENT FILLET WELDS. ALL WELDS LAID IN FLAT POSITION	32.9	44.8	21.8	9.4
44Cd	I-BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH $\frac{1}{4}$-IN. INTERMITTENT FILLET WELDS. BOTTOM PLATE LONGER THAN TOP PLATE		38.9	16.5	11.5 †
44Ce	I-BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{16}$-IN. INTERMITTENT FILLET WELDS. BOTTOM PLATE LONGER THAN TOP PLATE			22.0	8.2 ‡
44Ja	I-BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{16}$-IN. INTERMITTENT FILLET WELDS. HEAVY WELD ACROSS ENDS			22.8	12.0
44Jc	I-BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{16}$-IN. CONTINUOUS FILLET WELDS. COVER PLATES TAPERED IN THICKNESS				14.2
43E 44E	I-BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{4}$-IN. DIAMETER RIVETS.	38.5	46.5	26.8●	12.4●
44Ha	WIDE FLANGE BEAM WITH INTERMEDIATE STIFFENERS WELDED TO WEB AND BOTH FLANGES.	42.3	47.2		18.4
44Hb	WIDE FLANGE BEAM WITH INTERMEDIATE STIFFENERS WELDED TO WEB AND COMPRESSION FLANGE				26.6
44Hc	WIDE FLANGE BEAM WITH INTERMEDIATE STIFFENERS WELDED TO COMPRESSION FLANGE AND TOP 12-IN. OF WEB				32.7
44Ma	I-BEAM WITH LATERAL PLATES ATTACHED TO BEAM WITH TRANSVERSE FILLET WELDS.			19.2	12.2
44Mb	I-BEAM WITH LATERAL PLATES ATTACHED TO BEAM WITH LONGITUDINAL WELDS			24.4	13.6
44Mc	I-BEAM WITH LATERAL PLATES ATTACHED TO BEAM WITH $\frac{3}{4}$-IN DIAMETER RIVETS.		48.7●	26.4●	14.6 ●
B	I-BEAM WITH BUTT WELDED SPLICES WITH WELD REINFORCEMENT ON.				16.0
C	I-BEAM WITH BUTT WELDED SPLICES WITH WELD REINFORCEMENT MACHINED OFF.				16.7
D	I-BEAM WITH RIVETED SPLICE AND PARTIAL LENGTH COVER PLATES.				18.0 ●

Table 2—Relative Fatigue Strength of Various Types of Beams in Flexure

TEST SERIES	DESCRIPTION OF TEST SPECIMEN	SECTION MODULUS INCHES CUBED	SECTION MODULUS RATIO TO PLAIN BEAM	FATIGUE STRENGTH AT 2,000,000 CYCLES KIPS PER SQ. IN.	BENDING MOMENT CAPACITY AT 2,000,000 CYCLES KIP - FT.	CAPACITY RATIO TO PLAIN BEAM
COL. 1	2	3	4	5	6	7
44 A	I-BEAM WITHOUT REINFORCEMENT.	36.0	1.00	31.2	93.6	1.00
44Ga	FABRICATED BEAM WITH $\frac{1}{8}$-IN. CONTINUOUS FILLET WELD.	62.0	1.72	17.2	88.9	0.95
44Gb	FABRICATED BEAM WITH $\frac{3}{16}$-IN. CONTINUOUS FILLET WELD	62.0	1.72	16.6	85.8	0.92
44Ba	I-BEAM WITH FULL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{16}$-IN. CONTINUOUS FILLET WELD. ($\frac{3}{8}$-IN. BOTTOM PLATE)	61.2	1.70	22.8	116.3	1.24
A1-A3	I-BEAM WITH FULL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{16}$-IN. CONTINUOUS FILLET WELD ($\frac{1}{2}$-IN. BOTTOM PLATE)	75.2	2.09	23.4	146.6	1.57
A4-A6	I-BEAM WITH FULL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{16}$-IN. CONTINUOUS FILLET WELD. ($\frac{3}{8}$-IN. BOTTOM PLATE)	85.8	2.38	20.3	145.4	1.56
44Bb	I-BEAM WITH FULL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{16}$-IN. INTERMITTENT FILLET WELD	61.2	1.70	16.5	84.4	0.90
44EA	I-BEAM WITH FULL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{4}$-IN. DIAMETER RIVETS	61.0	1.69	15.8	80.5	0.86
44Bd	I-BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{16}$-IN. CONTINUOUS FILLET WELD ALONG EDGES. SPECIAL END CONDITION	61.2	1.70	10.0	51.0	0.54
44Cb	I-BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{16}$-IN. INTERMITTENT FILLET WELD ALONG EDGES AND ENDS	61.2	1.70	8.4	42.8	0.46
43 E 44 E	I-BEAM WITH PARTIAL LENGTH COVER PLATES ATTACHED WITH $\frac{3}{4}$-IN. DIAMETER RIVETS	61.0	1.69	12.4	66.9	0.67

the true stresses, inasmuch as the stress-strain relation changes at the proportional limit. Plastic flow was limited to a relatively short length of the flange, and the yield point was not so apparent as for the same steel under a static tension test. For some specimens there was a definite drop in the beam; for others there was none, but there was a definite slowing down in the rate of loading with the testing machine running at a uniform speed. The corresponding loads have been reported as the yield point. For other specimens, no yield point was detected with the method of testing used.

The yield point of the various specimens tested varied from 32,900 to 42,300 psi., but the ultimate strength of the specimens, which varied from 38,900 to 59,300 psi., was reduced by the unsupported top flange.

The average fatigue strengths shown in the last two columns of Table 1 are the average of the result obtained from tests on at least three specimens for each series. In general, the fracture occurred on the lower or tension flange, except for those specimens designed especially to produce failure in the compression flange, such as 44Cd and 44Ce.

The tests were planned to produce failure in some specimens at 100,000 cycles of load and failure in others at 2,000,000 cycles, but the specimens usually failed after a variable number of cycles of stress. The fatigue strength at 100,000 cycles, designated as F100,000, was then computed by means of an empirical equation, from tests in which the actual number of cycles of stress to produce failure was less than 600,000. The fatigue strength at 2,000,000 cycles, designated as F2,000,000, was computed from tests in which the actual number of cycles of stress to produce failure was greater than 300,000. The values of F100,000 and F2,000,000, were both computed from the tests in which the actual number of cycles of stress to produce failure was between 300,000 and 600,000.

Because of the small number of specimens tested of each type, the values reported for F100,000 and F2,000,000 must be considered as more or less approximate. However, it is believed that the values are sufficiently accurate to indicate the relative fatigue strengths of the various types tested.

The relative fatigue strength and moment resisting capacities of various types of beams normally used in short beam spans and stringers of bridges are shown in Table 2. The section moduli of the various specimens are shown in Col. 3 of this table, while the ratios of the section modulus to that of the plain beam are shown in Col. 4. The fatigue strengths determined for each series are shown in Col. 5. The bending moment capacity of each series at 2,000,000 cycles, as determined from the fatigue strength and section modulus of the specimen, is shown in Col. 6, while the ratios of the bending moment capacity to that of the plain beam are shown in Col. 7.

SIGNIFICANCE OF TEST RESULTS

A study of the fatigue strengths determined for the various types of specimens subjected to repeated cycles of stress clearly indicates the following significant features:

(*a*) The carrying capacity of the various types of beams does not increase directly with an increase in the section modulus of the beam. For example, it can be seen from Table 2 that while the section modulus of a rolled beam reinforced with cover plates fastened to the beam with $^3/_{16}$-in. continuous fillet welds (Series 44Ba) increased 70% over that of a plain beam (Series 44A), the bending moment capacity was only increased 24% over that of the plain beam.

(*b*) The carrying capacity of a beam with full length welded cover plates is increased by using heavier cover plates, but there appears to be a limit to this increase in the carrying capacity. For example, it can be seen from Table 2 that the $^1/_2$-in. cover plates on the specimens of Series A1-A3, increased the section modulus from 62.2 in.^{3} for the specimens of Series 44Ba with the $^3/_8$-in. plates, to 75.2 in.^{3}, or an increase of 23%. The carrying capacity increased from 116.3 kip. ft. to 146.6 kip. ft., or an increase of 26%. However, the increase in the carrying capacity of the specimens of Series A4-A6 with the $^5/_8$-in. cover plates was only 25% over that for Series 44Ba, while the section modulus increased 40%, indicating that there is some economic relation between the cover-plate area and the flange area of the beam.

(*c*) The carrying capacity of a beam with partial length cover plates is decreased tremendously under that for a beam fabricated with full length cover plates. A comparison of the carrying capacity of the two types with welded cover plates is shown in Table 2 for series 44Ba and 44Bd. The section moduli of the specimens of the two series are the same and the specimens would normally be expected to carry the same static load, but their carrying capacity, when subjected to repeated loads, is entirely different. The capacity of the beam with the full length cover plate is 116.3 kip. ft., while that for the beam with the partial length cover plate is only 51.0 kip. ft., indicating that the cutting off of the plate reduced the capacity by 56%.

The reduction in the carrying capacity of beams with riveted cover plates, due to cutting off the plates, is not as great as that occurring for welded cover plates, as can be seen by comparing the value for series 44EA and 43E, Table 2. The bending moment capacities for the two series indicate that the cutting off of the plates reduced the carrying capacity by only 17%.

It is evident from the results of these tests that whenever it becomes necessary to attach cover plates to beams, the plates should be extended past the theoretical cut-off point at least a sufficient distance so that the stress in the beam at the end of the cover plate is below the fatigue strength. A method of calculating the correct cover plate lengths for a 50-ft. beam span is shown in Fig. 6 and for a 100-ft. girder span in Fig. 7. The resisting moment of the 36-in. beam and two 12-in. by $^1/_2$-in. continuously welded cover plates, when subjected to repeated cycles of stress of 22,800 psi., is calculated as 2320 ft. kip., as shown in Fig. 6. Under static loading conditions, the cover plates could be stopped at the calculated cut-off point, but under re-

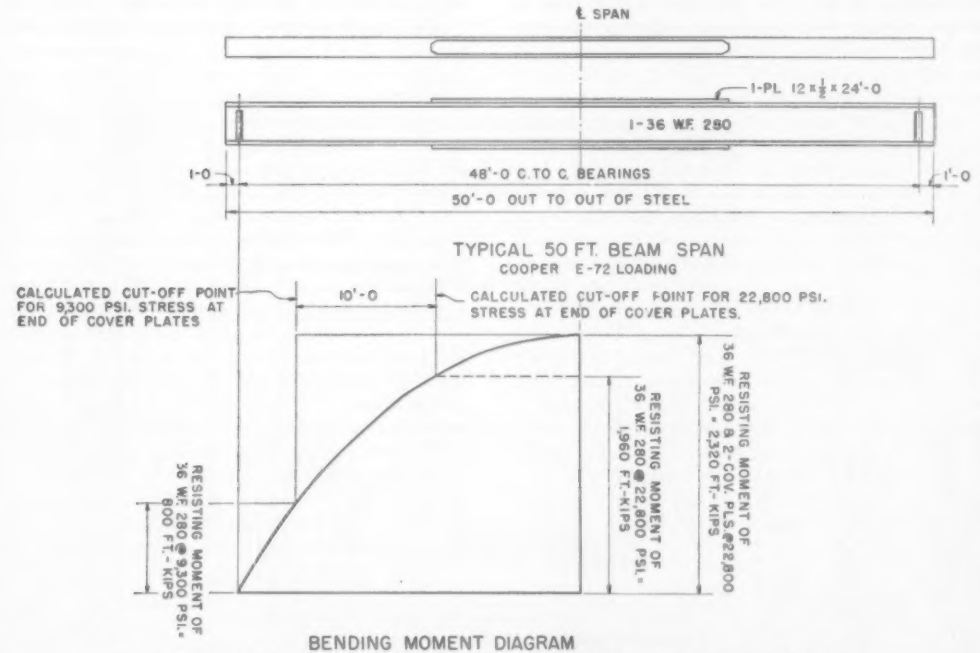
℄ SPAN
1-PL 12 x ½ x 24'-0
1-36 WF 280
1'-0
48'-0 C. TO C. BEARINGS
1'-0
50'-0 OUT TO OUT OF STEEL
TYPICAL 50 FT. BEAM SPAN
COOPER E-72 LOADING
CALCULATED CUT-OFF POINT FOR 9,300 PSI. STRESS AT END OF COVER PLATES
10'-0
CALCULATED CUT-OFF POINT FOR 22,800 PSI. STRESS AT END OF COVER PLATES.
RESISTING MOMENT OF 36 WF 280 @ 9,300 PSI = 800 FT.-KIPS
RESISTING MOMENT OF 36 WF 280 @ 22,800 PSI = 1,960 FT.-KIPS
RESISTING MOMENT OF 36 WF 280 & 2-COV. PLS. @ 22,800 PSI. = 2,320 FT.-KIPS
BENDING MOMENT DIAGRAM

Fig. 6 Determination of cover-plate lengths, 50-ft. welded beam span, ballasted timber floor

peated loading it is necessary to extend the plates 10 ft. past this point, or to a point where the stress in the beam is only 9100 psi. It is evident that a heavier 36-in. beam would be more economical; in fact, the carrying capacity of the same weight beam without cover plates is greater than the reinforced beam on account of the higher fatigue strength.

The method of calculating the cover plate lengths on riveted spans, when subjected to repeated cycles of load, is shown on Fig. 7. It can be seen from the moment diagram that it becomes necessary to extend the outside cover plates a considerable distance past the theoretical cut-off point, but the distance decreases for the other plates. It should be kept in mind that the unit stress of 12,400 psi., used on Fig. 7, was for ready identification taken from test series 43E and 44E, Table 2. In the actual design of a long girder span, it would be necessary to consider: (1) that because of its dead load, the actual stress cycle affecting fatigue strength is somewhat less than zero-to-maximum tension; (2) the probable repetitions of maximum loading would never attain 2,000,000; and (3) conversely, the unit stress of 12,400 psi. represents failure and does not contain any safety margin. The scant data from these tests would need to be interpreted and applied with judgment.

(*d*) The various tests conducted on specimens with partial length cover plates fastened to the beams with welds but with different plate and weld end conditions, indicate that there is no practical method to increase the fatigue strength of beams with partial length cover plates. The only significant increase in fatigue strength was obtained by tapering the cover plates in thickness and then fastening the plates to the beam with tapered welds on the edges, and with long tapered welds at the ends of the plates, but even for this method the fatigue strength was only increased to 14,200 psi., see Table 1, Series 44Jc.

The tests conducted on the specimens of series 44Ca and 44Caa indicate that the welds across the ends of the cover plates do not reduce the fatigue strength of the specimens. The average fatigue strength at 2,000,000 cycles for four specimens with welds across the ends of the plates was determined as 9300 psi., while the average value for three specimens without welds across the ends of the plates was only 8900 psi.

(*e*) The results of the tests conducted on the specimens of series 44Cd and 44Ce indicate that the fatigue strength of the compression flanges of beams is about the same as that of the tension flange.

(*f*) The fatigue strength at 100,000 cycles is about the same for specimens fabricated with continuous fillet welds as that for intermittent fillet welds, see Table 1, series 44Ba and 44Bb. However, the intermittent fillet welds materially reduce the fatigue strength at 2,000,000 cycles, with failure occurring at the end of the weld bead.

(*g*) The results of the tests on beams with butt-welded splices, series B and C, Table 1, indicate that the fatigue strength at 2,000,000 cycles is only about half that of a plain beam, Series 55A. It appears that the reduction in the fatigue strength is due principally to the slag inclusions in the butt welds, as the strength was only slightly increased by machining off the weld reinforcement flush with the base metal.

(*h*) The efficiency of a riveted spliced beam can be

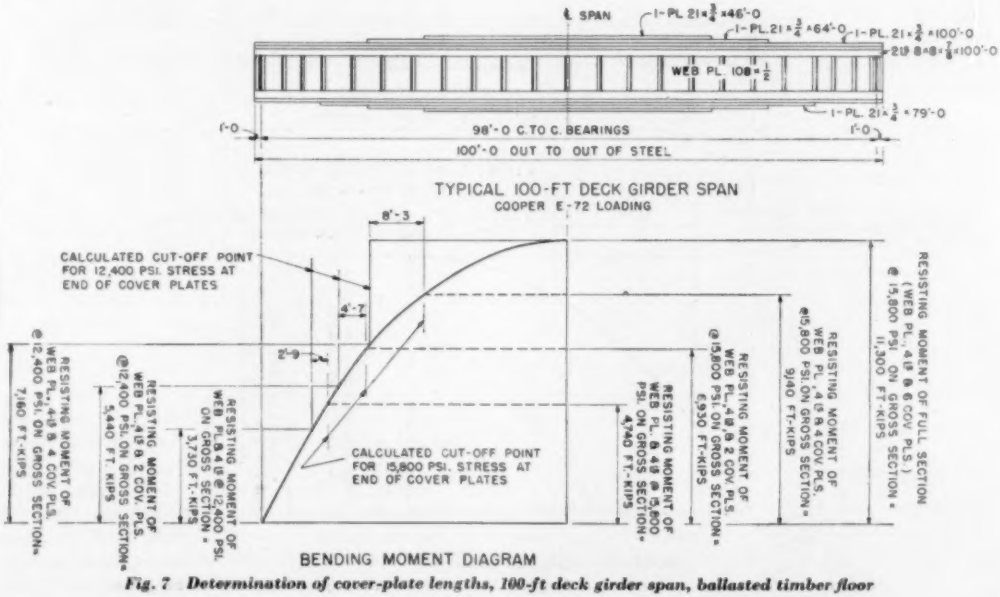
℄ SPAN
1-PL 21×¾×46'-0
1-PL 21×¾×64'-0
1-PL 21×¾×100'-0
2 @ 8"=8×¾×100'-0
WEB PL 108×½
1-PL 21×¾×79'-0
1'-0
98'-0 C. TO C. BEARINGS
1'-0
100'-0 OUT TO OUT OF STEEL
TYPICAL 100-FT DECK GIRDER SPAN
COOPER E-72 LOADING
8'-3
CALCULATED CUT-OFF POINT FOR 12,400 PSI. STRESS AT END OF COVER PLATES
4'-7
2'-9
CALCULATED CUT-OFF POINT FOR 15,800 PSI. STRESS AT END OF COVER PLATES
RESISTING MOMENT OF WEB PL. 108×½ & 4 COV. PLS. @ 12,400 PSI. ON GROSS SECTION = 7,160 FT.-KIPS
RESISTING MOMENT OF WEB PL. 108×½ & 2 COV. PLS. @ 12,400 PSI. ON GROSS SECTION = 5,440 FT. KIPS
RESISTING MOMENT OF WEB PL. 108×½ @ 12,400 PSI. ON GROSS SECTION = 3,730 FT.-KIPS
RESISTING MOMENT OF WEB PL. 108×½ @ 15,800 PSI. ON GROSS SECTION = 4,740 FT.-KIPS
RESISTING MOMENT OF WEB PL. 108×½ & 2 COV. PLS. @ 15,800 PSI. ON GROSS SECTION = 6,930 FT.-KIPS
RESISTING MOMENT OF WEB PL. 108×½ & 4 COV. PLS. @ 15,800 PSI. ON GROSS SECTION = 9,140 FT.-KIPS
RESISTING MOMENT OF FULL SECTION (WEB PL. 108×½ & 6 COV. PLS.) @ 15,800 PSI. ON GROSS SECTION = 11,300 FT.-KIPS
BENDING MOMENT DIAGRAM

Fig. 7 Determination of cover-plate lengths, 100-ft deck girder span, ballasted timber floor

secured by comparing the results obtained on the spliced specimens of Series D with those obtained on the unspliced specimens of Series 44A. The carrying capacity of the unspliced beam was 93.6 kip. ft., while that for the spliced beam was 66.4 kip. ft., indicating that a riveted splice reduces the carrying capacity by about 29%. However, the splicing of a beam already reinforced with riveted cover plates should not reduce its carrying capacity, as can be seen by comparing the fatigue strengths of Series 44EA and Series D, Table 1.

(*j*) The carrying capacity of beams is greatly reduced by the attachment of lateral plates to the flanges by either welding or riveting. For example, the fatigue strength of a plain beam was determined as 31,200 psi., see Table 1, Series 44A, but when lateral plates were fastened to the flanges the fatigue strength was reduced to 12,200 psi., or about 39% of the original strength, see Series 44Ma. The method of attaching the lateral plates to the beam flanges did not have much effect on the fatigue strength. In Series 44Mb, the plates were fastened with longitudinal fillet welds instead of transverse fillet welds, and in Series 44Mc the plates were riveted to the flanges, and for both methods the increase in strength was small.

(*k*) The carrying capacity of beams having the webs reinforced with vertical stiffeners at the points of maximum stress depended upon the method of attaching the stiffeners to the beam. In Series 44Hc the stiffeners were welded to the compression flange and to the top 12 in. of the web without any apparent reduction in the carrying capacity of the beam. In Series 44Hb the stiffeners were welded to the compression flange and to the full depth of web, with a reduction of about 19% in its carrying capacity. In Series 44Ha the stiffeners were welded to both the compression and tension flanges and to the full depth of web, with a reduction of 44% in its carrying capacity.

(*l*) It is evident from the results of tests obtained on the fabricated beams of series 44Ga and 44Gb that the carrying capacity of these beams is appreciably lower than the capacity of a rolled beam. For example, it can be seen from Table 2, that while the section modulus of the fabricated beam with $^3/_{16}$-in. continuous fillet welds, Series 44Gb, increased 72% over that for a rolled beam, Series 44A, the bending moment capacity reduced to 92% of that of the rolled beam.

(*m*) The results of the tests indicate that the welds have exceptionally high fatigue strengths when transmitting loads by horizontal shear. For example, the calculated stress in the welds connecting the flange plates to the web plate of one specimen of Series 44Ga was 27,600 psi., and still this specimen failed in the lower flange plate after 563,900 cycles of stress varying from about zero to 28,000 psi. However, it should be kept in mind that the calculated weld stress is based upon the throat area and does not include any of the weld area resulting from the root penetration.

(*n*) The fatigue strengths shown in Table 1 for the various series at 100,000 cycles of stress indicate that, in general, fatigue failures will not occur in the longer girder span bridges where the dead load stress comprises a fairly large percentage of the total stress. In addition, data are now available indicating that only a small percentage of the trains passing over railroad girder bridges produces stresses as large as the fatigue strength of these members.

CONCLUSIONS

Although these tests were too few in number, and too restricted in stress-cycle patterns, to afford a basis for close estimating of the fatigue life of bridge members under service conditions, they do lead to certain qualitative conclusions.

The customary working stresses and methods of design of reinforcements are adequate for flexural members designed to withstand not over 100,000 cycles of maximum stress range. For flexural members to be subjected to more than 100,000 cycles, the following precautions are recommended, and increasingly so as the probable number of cycles increases:

(*a*) Any plain-rolled beam without attachments or flange holes will have a greater fatigue life than any cover-plated beam, or any built-up beam, of the same or somewhat greater section modulus.

(*b*) If lack of depth dictates cover-plating of beams in new work, or if existing beams are cover-plated for reinforcement, the cover plates should preferably be of full length, and in any case long enough so that the stress in the main material ahead of the plates does not exceed for welded beams, 40% of the normal design stress for flanges, or for riveted beams, 70%.

(*c*) Continuous fillet welds afford the best means of attaching full length cover plates to beams; followed by rivets and by intermittent welds, in that order.

(*d*) Splices in continuous beams should be placed where they will be subjected to the lowest practicable bending moment. Flange splices in simple-span beams should not be permitted.

(*e*) Any attachment, riveted or welded, to a tension flange at a point of considerable bending moment will reduce the fatigue life. If such attachments to the flange cannot practicably be avoided, the allowable flange stress at such points should be reduced below usual working values. This precaution applies to stiffeners, lateral plates, diaphragm flanges, etc.

(*f*) For flexural members subjected to stresses of opposite sign, the loss of fatigue life due to any of the stress-raising conditions discussed herein would be still more serious.

Aircomatic Welding of Nickel and Stainless-Clad Steels

Procedures for welding nickel-clad and stainless-clad steels by manual and automatic means

by T. T. Watson and R. R. Rothermel

Introduction

Clad-steel plates are well-established engineering materials of construction. Such composite plate products generally consist of a commercial grade of carbon steel or a low-alloy high-strength steel to which a veneer or cladding metal is integrally and permanently bonded. The normal thickness of the clad metal is 10 or 20% of the total plate thickness. There are many reasons for using clad steels and undoubtedly the most important is that they are less expensive than the solid cladding metal and yet have the same corrosion-resistance properties. It is, therefore, important that the welds on the clad metal side have the same corrosion resistance as the cladding metal. Some of the types of clad plates produced by Lukens Steel Co. are nickel, Monel, Inconel and all types of stainless steels.

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MANUAL WELDING

To obtain the desired corrosion resistance and good physical properties in clad-steel plate welds, a precise procedure is used in manual welding. Using the normal joint preparation for clad steel the backing steel is welded first with a mild steel electrode. The joint is back chipped and the clad side is welded with a suitable electrode. It is important that the welds on the clad side have corrosion resistance similar to the clad metal and this is achieved by selection of proper electrodes or by increasing the number of passes.

In the case of stainless-clad steels, higher alloy electrodes are used while with nickel-clad steel the iron dilution is kept to a minimum by increasing the number of passes.

In the past, most clad-steel welded fabrication has been done using the manual electric-arc process. However, certain commercial products of clad steel such as pipe and long cylindrical vessels are economically better suited to automatic welding. For this reason, a thorough investigation was made of the Aircomatic welding process and its possible utilization in the clad-steel field.

THE AIRCOMATIC PROCESS

The Aircomatic process, a development of the Air Reduction Sales Co., is a process wherein a consumable electrode, consisting of the filler metal, is fed into the arc stream which is completely enveloped by an inert atmosphere. The equipment can be operated manually, semiautomatically or automatically, and in the case of automatic operation, either the sections to be welded or the welding head can be attached to a radiograph to provide forward motion.

Much literature has been published recently concerning the Aircomatic process. A more detailed description of the process can be found in one of the more recent articles.*

AIRCOMATIC WELDING OF NICKEL-CLAD STEEL

In the experiments with nickel-clad steel, weld tests were made on plates of 10 and 20% cladding and ranging in thickness from 1/4 to 3/4 in.

One group of tests was conducted on 1/4- and 3/8-in. gage plates which had a square butt-joint design with a 1/32-in. root opening. These plates were welded entirely by the Aircomatic process. The first weld was deposited from the carbon steel side with No. 61 nickel wire and with the aim of obtaining penetration at least

* Muller, A., Gibson, G. J., and Roper, E. H., "The Aircomatic Welding Process," THE WELDING JOURNAL, **29** (6), 458–482 (1950).

Table 1—Conditions Used in Making Weld Tests on Nickel-Clad Steel with the Aircomatic Process

Plate gage, in.	*Cladding, %*	*No. of weld passes*	*Side from which weld was deposited*	*Current, amp.*	*Voltage*	*Travel speed, in. per minute*	*Gas*	*Gas flow, cu. ft. per hour*	*Type wire*	*Wire diameter, in.*
1/4	10	Two	Steel	370	29	14	Argon	90	61 Ni	1/16
			Clad	300	24	38	Argon	90	61 Ni	1/16
1/4	20	Two	Steel	380	27	14	Argon	90	61 Ni	1/16
			Clad	305	24	38	Argon	90	61 Ni	1/16
3/8	10	One	Steel	555	33–34	17	Argon	90	61 Ni	0.080
3/8	10	One	Steel	700	33–34	22	Argon	90	61 Ni	5/64
3/8	10	Two	Steel	490	32–33	17	Argon	90	61 Ni	0.080
			Clad	350	29–30	30	Argon	90	61 Ni	1/16
1/2	10	Two	Clad	300	22	36	Argon	90	61 Ni	1/16
			Clad	300	22	36	Argon	90	61 Ni	1/16
1/2	20	Two	Clad	300	23	36	Argon	90	61 Ni	1/16
			Clad	340	24	18	Argon	90	61 Ni	1/16
3/4	20	Two	Clad	310	23	22	Argon	90	61 Ni	1/16
			Clad	325	24	16	Argon	90	61 Ni	1/16

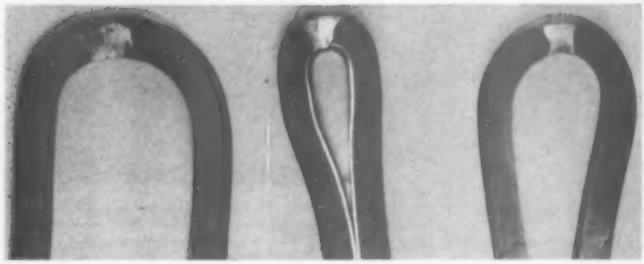


Fig. 1 Bends made of weld test plate of nickel-clad steel 3/8 in. gage, 10% clad, welded in one pass with No. 61 nickel wire

to the bond line but not exceeding the surface on the clad side. The weld was then completed by depositing a bead on the clad side, again using No. 61 nickel wire.

In test plates exceeding 3/8 in. in thickness, a beveled joint was used and the steel side was welded manually with mild-steel electrodes. The weld was then completed by back chipping from the clad side and depositing a bead of No. 61 nickel wire by the Aircomatic process. The best conditions for welding plates of various thicknesses are shown in Table 1.

The mechanical properties of these welds were determined by means of free-bend and side-bend tests of the type shown in Fig. 1. As shown in Table 2, the welds elongated 35–50% on the steel side and 30–40% on the clad side.

weld was ascertained to be 3–4%. In obtaining samples for these analyses, chips were milled from the surface of the clad side weld to just above the surface of the cladding. Table 3 lists chemical analyses of welds.

AIRCOMATIC WELDING OF STAINLESS-CLAD STEEL

Austenitic Group

Types 304 and 347 stainless-clad steel with 10–20% cladding and varying in thickness from 3/8 to 3/4 in., were selected for Aircomatic weld tests. On plates of 3/8-in. gage, a square butt joint was used, while on plates of heavier gage a beveled joint was used.

On 3/8-in. gage test plates of both stainless types 304 and 347, an initial bead was deposited from the backing steel side with 25 Cr–20 Ni wire, and the final bead was deposited from the stainless-clad side. On Type 304 stainless-clad steel, final beads were made with 25 Cr–20 Ni wire and with 19 Cr–9 Ni wire, while 19 Cr–9 NiCb wire was used to deposit the clad-side bead in the Type 347 stainless-clad steel. Welding conditions for this group are shown in Table 4.

Welds made on stainless Type 304 clad steel showed good ductility with 30–45% elongation on the backing steel side. Complete bend test results are shown in Table 5 and a photograph of a bend test is shown in Fig. 2.

Bend tests of the stainless Type 347 elongated 45–50% on the steel side and 50–55% on the clad side. These results are given in Table 5 and a photograph of a bend test is shown in Fig. 3.

Chemical analyses of the welding wire and clad metal used in these tests are given in Table 6 and of the welds deposited on the clad side in Table 7.

These analyses show that the chromium-nickel content of the weld on the clad side of Type 304, when welded with 25 Cr–20 Ni wire, compares favorably with that of the clad metal.

Table 2—Bend Test Results of Nickel-Clad Steel Weld

Plate gage, in.	*Cladding, %*	*No. of weld passes*	*Side from which weld was deposited*	*Type wire*	*—Free bend, elongation, %— Steel in tension*	*Clad in tension*	*Side bend*
1/4	10	Two	Steel	61 Ni	45.4, 48.0	....	Bent satisfactorily
			Clad	61 Ni	.....	37.5, 39.0	
1/4	20	Two	Steel	61 Ni	33.3, 37.2	....	Bent satisfactorily
			Clad	61 Ni	.....	36.8, 38.0	
3/8	10	One	Steel	61 Ni	37.5, 41.6	Bent satisfactorily	Bent satisfactorily
3/8	10	Two	Steel	61 Ni	36.0, 39.2	....	Bent satisfactorily
			Clad	61 Ni	.....	38.0, 39.6	

As has been mentioned earlier, the weld deposited on the clad side must have the same corrosion-resistant properties as the clad metal. The iron content of a nickel weld deposited with the Aircomatic process compares favorably with the iron content of a nickel bead deposited manually. However, the iron content of the top layer of the weld on the clad side is a function of the number of passes deposited on the clad side. In the case where one pass was deposited on the clad side (3/8-in. gage plate, 10% clad) the iron content of the weld was found to be 5–7%. Where three passes were deposited on the clad side, as in the welding of 3/4-in. gage 20% nickel-clad steel, the iron content of the

Table 3—Chemical Analyses of Welds Deposited on Nickel-Clad Side

Plate gage, in.	*Cladding, %*	*No. of weld passes*	*Side from which weld was deposited*	*Type wire*	*Clad-Weld analysis, Fe*
1/4	20	Two	Steel	61 Ni	...
			Clad	61 Ni	5.45
3/8	10	Two	Steel	61 Ni	...
			Clad	61 Ni	7.11
1/2	20	Two	Clad	61 Ni	...
			Clad	61 Ni	6.78
3/4	20	Two	Clad	61 Ni	...
			Clad	61 Ni	6.61
3/4	20	Three	Clad	61 Ni	3.72

WELDING OF CLAD STEEL FROM THE BACKING-STEEL SIDE

Manual Welding

There are occasions, however, when clad-steel weldments are difficult to weld in the conventional manner. The dimensions of the weldment might be such that an operator or equipment is unable to weld the clad side from the interior of such weldments; therefore, the joint design and procedure must be altered accordingly to permit the deposition of a weld in its entirety from the outside, or backing-steel side.

When welded manually, the root gap is widened enough to allow the operator to insert the electrode to the root of the joint. The first pass is deposited against a grooved copper-backing bar with an electrode similar in composition to that of the cladding metal. Then, other passes are deposited, using the same electrode, until the joint is welded. It is unwise to finish welding the joint with a mild steel elec-

Fig. 2 Bends made on Type 304 clad-steel test plate, 3/8 in. gage, 20% clad, welded with 25 Cr–20 Ni wire

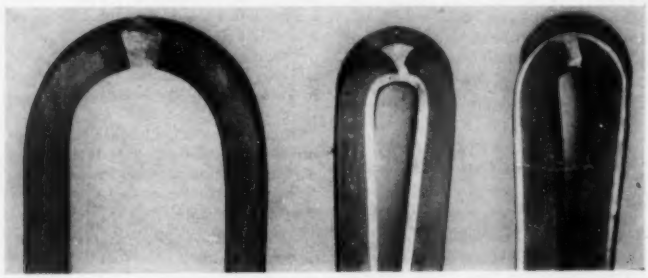


Fig. 3 Bends made on Type 347 clad-steel test plate, 3/8 in. gage, 20% clad

Plate was welded from steel side with 25 Cr-20 Ni wire and from clad side with 19 Cr-9 Ni Cb wire

trode. When steel is welded on alloy, "pickup" ranges from 10 to 40% and the cooling rates of the weld are such that a hard brittle material is formed. Consequently, the ductility of the weld is lower than if the weld is made entirely with an alloy electrode.

SINGLE-PASS AIRCOMATIC WELDING OF NICKEL-CLAD STEEL

It was decided to determine if this type of weld could be deposited in one pass from the carbon steel side, using the Aircomatic process.

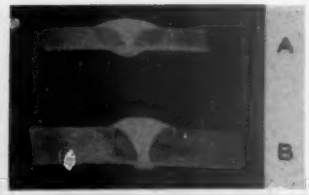


Fig. 4 Cross sections of welds made on nickel-clad steel, welded with No. 61 nickel wire

(a) is 1/4 in. gage, 20% nickel-clad steel welded in two passes, the first being from the steel side. (b) is 3/8 in. gage, 10% nickel-clad steel welded in one pass from the steel side

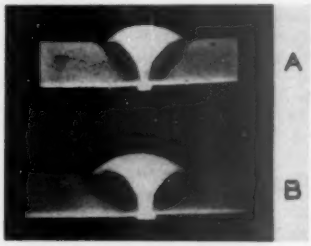


Fig. 5 Cross sections of welds made in one pass

(a) is Type 304 clad steel, 1/4 in. gage, 10% clad welded with 25 Cr-20 Ni wire. (b) is Type 410 clad steel, 1/4 in. gage, 10% clad, welded with 25 Cr-20 Ni wire

Complete welds were made in one pass from the carbon steel side using No. 61 nickel wire; the conditions used in making this type of weld are shown in Table 1. Cross sections of single-pass welds are shown in Figs. 4 and 5 in which can be seen the small amount of weld reinforcement obtained on the clad side. In applications

Table 4—Conditions Used in Making Weld Tests on Stainless-Clad Steel with the Aircomatic Process

Type Clad	Plate gage, in.	Cladding %	No. of weld passes	Side from which weld was deposited	Current, amp.	Voltage	Travel speed in. per minute	Gas	Gas flow, cu. ft. per hour	Type wire	Wire diameter, in.
304	3/8	10	One	Steel	500	29–30	18	Argon	90	25 Cr–20 Ni	5/64
304	3/8	10	One	Steel	500	32	14	Argon	90	19 Cr–9 Ni	5/64
304	3/8	10	One	Steel	500	30	14	60 A 40 He	.. ..	25 Cr–20 Ni	5/64
304	3/8	10	One	Steel	500	30	14	75 A 25 He	.. ..	25 Cr–20 Ni	5/64
304	3/8	10	One	Steel	500	30	14	90 A 10 He	.. ..	25 Cr–20 Ni	5/64
304	3/8	10	Two	Steel	500	29–30	18	Argon	90	25 Cr–20 Ni	5/64
				Clad	270	21–22	29	Argon	90	25 Cr–20 Ni	5/64
304	3/8	10	Two	Steel	500	29–30	18	Argon	90	25 Cr–20 Ni	5/64
				Clad	270	21–22	29	Argon	90	19 Cr–9 Ni	5/64
304	3/8	20	One	Steel	500	31	14	Argon	90	25 Cr–20 Ni	5/64
304	1/2	20	Two	Clad	360	24	29.0	Argon	90	25 Cr–20 Ni	5/64
				Clad	360	24	18.0	Argon	90	25 Cr–20 Ni	5/64
304	1/2	20	Two	Clad	360	24	30.0	Argon	90	25 Cr–20 Ni	5/64
				Clad	360	24	14.0	Argon	90	19 Cr–9 Ni	5/64
304	1	20	Two	Clad	390	25	14	Argon	90	19 Cr–9 Ni	5/64
				Clad	390	25	14	Argon	90	19 Cr–9 Ni	5/64
347	3/8	20	Two	Steel	490	30–31	14	Argon	90	25 Cr–20 Ni	5/64
				Clad	370	29–30	38	Argon	90	19 Cr–9 NiCb	1/16
410	3/8	10	One	Steel	500	29–30	18	Argon	90	25 Cr–20 Ni	5/64
410	3/8	10	One	Steel	500	29	15	Argon	90	25 Cr–20 Ni	5/64

Table 5—Bend-Test Results of Stainless-Clad Steel Weld

Type clad	Plate gage, in.	Cladding, %	No. of weld passes	Side from which weld was deposited	Type wire	Free Bend, Elongation, % — Steel in tension	Free Bend, Elongation, % — Clad in tension	Side bend	Postweld treatment
304	3/8	10	One	Steel	25 Cr–20 Ni	38.5, 40.0	Bent satisfactorily	Bent satisfactorily	None
304	3/8	10	One	Steel	19 Cr–9 Ni	4.0, 4.0	Bent satisfactorily	Did not bend	None
304	3/8	10	One	Steel (60A, 40 He)	25 Cr–20 Ni	20.4, 28.0	Bent satisfactorily	Bent satisfactorily	None
304	3/8	10	One	Steel (75A, 25 He)	25 Cr–20 Ni	20.7, 21.6	Bent satisfactorily	Bent satisfactorily	None
304	3/8	10	One	Steel (90 A, 10 He)	25 Cr–20 Ni	36.7, 37.5	Bent satisfactorily	Bent satisfactorily	None
304	3/8	20	One	Steel	25 Cr–20 Ni	42.3, 46.1	Bent satisfactorily	Bent satisfactorily	None
347	3/8	20	Two	Steel	25 Cr–20 Ni	48.2, 50.0	....	Bent satisfactorily	None
				Clad	19 Cr–9 NiCb	...	55.5, 57.1		
410	3/8	10	One	Steel	25 Cr–20 Ni	31.2, 33.4	Bent satisfactorily	Bent satisfactorily	S.R. 1150–1175° F.–1 hr. F. C.
410	3/8	10	One	Steel	25 Cr–20 Ni	31.4, 35.0	Did not bend	Bent satisfactorily	None

Table 6—Chemical Analyses of Welding Wire- and Clad-Metal

	Cr	Ni	Cb	Mn	C
25 Cr–20 Ni wire	26.34	22.00	..	..	..
19 Cr–9 Ni wire	19.64	10.00	..	..	..
19 Cr–9 Ni Cb wire	19.11	10.00	0.94	1.70	0.042
Stainless Type 304 clad	18.28	9.90	..	..	..
Stainless Type 410 clad	12.06	0.27	..	..	..
Stainless Type 347 clad	18.25	11.89	0.89	1.59	0.062

where continuity of surface is required, as in pipe fabrication, a minimum amount of grinding is required to remove the reinforcement.

The welds were tested by subjecting them to free-bend and side-bend tests. Because of the high-current densities employed, single-pass welds deposited by the Aircomatic process are narrow in the root, the welds being in the order of 1/8 in. wide at this point. Hence, while elongations can be measured on the steel side, the clad-side weld can only be adjudged by the degree to which it is bent. Bend-test results are given in Table 2 where it is shown that 35–50% elongation was obtained on the steel side while the clad side was bent through 180°.

SINGLE-PASS AIRCOMATIC WELDING OF STAINLESS-CLAD STEEL

Austenitic Group

Complete welds, deposited in one pass from the carbon-steel side, were made on test plates of Type 304 clad steel with 25 Cr–20 Ni and with 19 Cr–9 Ni wire. Welding conditions of this group of tests are shown in Table 4, while Fig. 5 shows cross sections of one pass welds.

Very low ductility values were obtained from the welds made with 19 Cr–9 Ni wire as values of 5% elongation were indicated. However, higher ductilities of 35–40% elongation were obtained on the steel side of welds made with 25 Cr–20 Ni wire. Figure 2 is a photograph of bends obtained from a single-pass weld made on Type 304 clad steel using 25 Cr–20 Ni welding wire, while Table 5 lists bend-test results.

A microhardness survey was made to compare the hardnesses of the welds made with 25 Cr–20 Ni wire and with 19 Cr–9 Ni wire. A weld made with the 19 Cr–9 Ni wire had Knoop hardness values of 450–480 (44–48 R_c) on the steel side gradually decreasing to values of 250–275 KHN (17–22 R_c) on the clad side. A weld made with 25 Cr–20 Ni wire showed values of 180–225 KHN (10–12 R_c) across the weld from the steel side to the clad side. Figure 6 shows a comparison of the hardness of welds made with 25 Cr–20 Ni wire and with 19 Cr–9 Ni wire.

Martensitic Group

Complete welds deposited in one pass were made on test plates of stainless Type 410 clad steel using 5/16-in. diameter 25 Cr–20 Ni wire. Welding conditions used in making these tests are shown in Table 4.

Table 7—Chemical Analyses of Welds Deposited on Stainless-Clad Side

Type clad	Plate gage, in.	Cladding, %	No. of weld passes	Side from which weld was deposited	Type wire	Weld analysis — Cr	Ni	Cb	Mn	C
304	3/8	10	Two	Steel	25 Cr–20 Ni	....	....	..	..	....
				Clad	25 Cr–20 Ni	23.04	18.18	..	..	....
304	3/8	20	One	Steel	25 Cr–20 Ni	17.70	12.60	..	..	....
304	3/8	20	Two	Steel	25 Cr–20 Ni	....	....	..	..	....
				Clad	19 Cr–9 Ni	18.25	10.60	..	..	....
304	1/2	20	Two	Clad	25 Cr–20 Ni	....	....	..	..	....
				Clad	25 Cr–20 Ni	22.95	19.88	..	..	....
304	1/2	20	Two	Clad	25 Cr–20 Ni	....	....	..	..	....
				Clad	19 Cr–9 Ni	18.87	12.66	..	..	....
304	1	20	Two	Clad	19 Cr–9 Ni	....	....	..	..	....
				Clad	19 Cr–9 Ni	17.31	10.31	..	..	....
347	3/8	20	Two	Steel	25 Cr–20 Ni	....	....	..	..	....
				Clad	19 Cr–9 NiCb	18.30	10.77	0.72	1.58	0.060
410	3/8	10	One	Steel	25 Cr–20 Ni	17.70	14.70	..	..	....
410	3/8	10	One	Steel	25 Cr–20 Ni	18.28	15.30	..	..	....

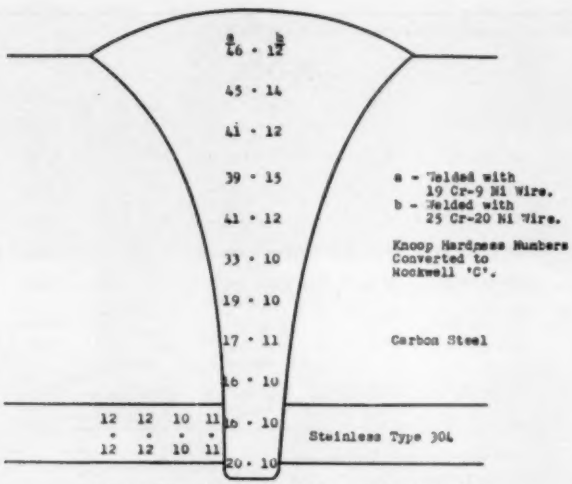


Fig. 6 Type 304 clad steel 1/8 in. gage—10% clad

Fig. 7 Bends made on Type 410 clad-steel test plate, 1/8 in. gage, 10% clad

Plate was welded from the steel side in one pass with 25 Cr-20 Ni wire

Inasmuch as stainless Type 410 is an air-hardening alloy, the welds on the clad side failed in bending before a 30° bend was obtained although the welds on the steel side had 30–35% elongation. After stress relieving at 1150–1175° F. for 1 hr., the welds on the clad side bent through 180° while the steel weld again had 30–35% elongation. Figure 7 is a photograph of bends obtained in the welding of Type 410 clad steel while Table 5 lists the bend-test results.

Microhardness surveys were made on welds in the as-welded condition and stress-relieved condition to compare the hardness of the welds and of the clad metal. In both the as-welded and stress-relieved conditions, Knoop hardness values of 200–240 (11–15 R_c) were noted in the weld. A maximum hardness of 420 KHN (40 R_c) was noted in the clad metal of the as-welded specimen, while a maximum hardness of 280 KHN (22 R_c) was noted in the clad metal of the stress-relieved specimen.

Figure 8 is a photomicrograph of a section of the weld across which a hardness traverse was taken, while Fig. 9 shows the comparison of the hardness of the weld and clad metal in the as-welded and stress-relieved conditions.

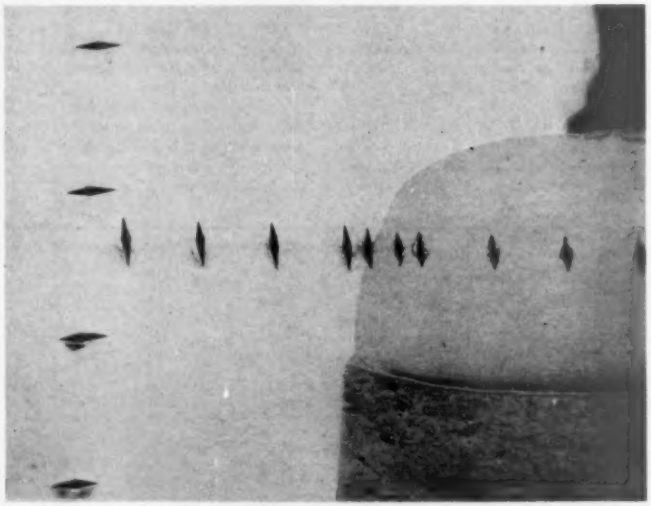


Fig. 8 Photomicrograph on section of weld made on Type 410 clad steel with 25 Cr–20 Ni wire

Plate is in as-welded condition Microhardness survey was made with Knoop indenter employing a 500 gram load. Values of 220–240 KHN were obtained in weld, with values of 400–420 KHN being obtained in the clad metal

VARIABLES ENCOUNTERED IN AIRCOMATIC AUTOMATIC WELDING

During the course of conducting the weld tests, it was noted that certain factors had to be closely controlled to obtain optimum properties. These factors are discussed in succeeding paragraphs.

JOINT DESIGN

Welds made manually or by most automatic methods are deposited in a U- or V-type of weld joint. However, in making welds with the Aircomatic process, it was found to be more advantageous to use a square butt joint with a 1/32-in. root gap as the high-current densities obtained in this process produce a weld of considerable penetration. Occasionally, when making a weld on the clad side for purposes of low dilution, a chipped or milled U-groove is used as more passes can be deposited in this type of joint than in the square-butt type.

A copper backing bar was used when making a complete weld, with 0.040-in. thick spacers separating the test plate from the backing bar to insure a sufficient weld reinforcement on the clad side.

WIRE AND WIRE SIZE

The composition of the wire used during welding depended upon the type of clad metal. International Nickel Co. designation No. 61 nickel wire was found to be best suited for the welding of nickel-clad steel.

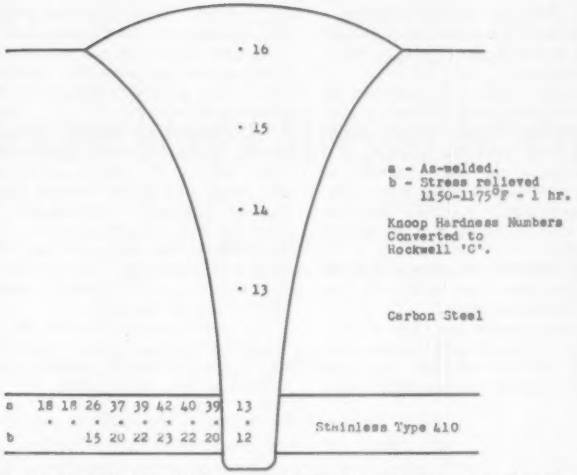

· 16
· 15
· 14
· 13
a - As-welded.
b - Stress relieved 1150-1175°F - 1 hr.
Knoop Hardness Numbers Converted to Rockwell 'C'.
Carbon Steel
a 18 18 26 37 39 42 40 39 13
b 15 20 22 23 22 20 12
Stainless Type 410

Fig. 9 Type 410 clad steel, 1/8 in. gage—10% clad, welded with 25 Cr–20 Ni wire

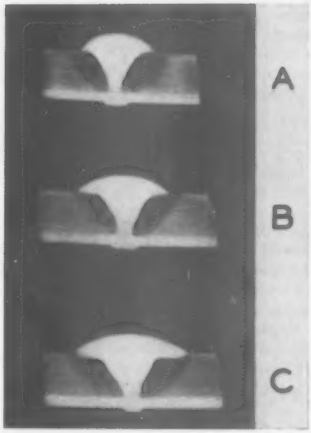


Fig. 10 Cross sections of welds made on Type 304 clad steel. 1/8 in. gage, 20% clad, showing effect of helium in inert-gas mixture

The following gas mixtures were used: (a) 10 cfh. helium, 90 cfh. argon, (b) 25 cfh helium, 75 cfh. argon, (c) 40 cfh. helium, 60 cfh. argon

Welds deposited on the steel side of stainless types 304, 347 and 410 were made with 25 Cr–20 Ni wire. It was found that using 19 Cr–9 Ni wire produced welds of low ductility. The clad side of stainless Type 304 was welded with 25 Cr–20 Ni wire while 19 Cr-9-NiCb wire was used to weld the clad side of stainless Type 347.

The diameter of the wire used in the welding process depended upon the gage of the plate and depth of penetration desired. When the gage of the plate was 1/8 in. and a complete weld in one pass was desired, the diameter of the wire used was 5/64 in. When a weld deposit of shallow penetration was desired, 1/16-in. diameter wire was employed.

WELDING CURRENTS AND TRAVEL SPEEDS

The welding current and travel speed of the radiograph to which the Aircomatic head was attached depended upon the depth of weld penetration desired. The following combinations of current, wire diameter and travel speeds were found to be optimum. When making a complete weld in one pass on a 1/8-in. gage plate, square butt joint, currents of 500–550 amp., direct current reversed polarity were employed with 5/64-in. diameter wire. Corresponding travel speeds were 14–17 in. per minute. When depositing a weld 1/16 to 1/8 in. deep, using 1/16-in. diameter wire, current values were 350–425 amp., while travel speeds were 25–40 in. per minute.

GAS AND GAS FLOW

Two gases, argon and helium, were used in the inert-gas-shielded welding process. Of the two, it was found that helium, having a higher ionization potential, produced a weld that was flatter in appearance and of deeper penetration. At the same time, it was found that helium produced more spatter loss than argon.

Combinations of argon and helium were also used in making weld tests. It was found that increasing the helium content of the mixture from 10 cu. ft. per hour (in a total mixture of 100 cu. ft. per hour) to 25 cu. ft. per hour reduced the ductility of the weld on the steel side from 36–38% elongation to 20–22% elongation. It was also found that the 90 cfh. argon–10 cfh. helium mixture produced a more stable arc, i.e., reduced the tendency for arc blow during welding, than when straight argon was used during welding.

Cross sections of welds made with combinations of argon and helium are shown in Fig. 10 where the effect upon weld appearance and weld penetration of increasing helium flow in the inert-gas mixture can be seen.

Adequate gas coverage was provided when a flow of 90–100 cu. ft. per hour of inert gas was obtained. A flow of 90 cu. ft. per hour of argon and 10 cu. ft. per hour of helium was used when providing a mixed inert-gas shield.

SHIELD BREAKDOWN AND SHIELD PROTECTION

The success of a weld deposited by the Aircomatic process depends upon the complete envelopment of the molten metal by the inert atmosphere. When the gaseous envelope of the arc is disturbed or destroyed, shield breakdown occurs. Impurities such as oxides, hydrocarbon deposits or dirt, in the weld area, can cause this shield breakdown. Hence, prior to welding, the edges and surfaces adjacent to the weld area were ground with a fine-abrasive grinding pad. Drafts, likewise, can destroy the inert-gas coverage. When this happens, oxygen and nitrogen from the atmosphere penetrate to the molten pool and cause the weld deposit to crack and be porous or undercut occurs on the plate. Also, arc voltage becomes unsteady and the depth of penetration of the weld deposit varies considerably.

It was found during the course of welding that more adequate gas coverage was provided if a shield was attached to the end of the "barrel" of the head. This permitted a weld deposit to remain blanketed with the inert gas while the welding process continued.

ARC VOLTAGE

In the Aircomatic process, for a given setting of current and travel speed, the depth of penetration is controlled by the arc voltage. If shallow penetration is desired, a long arc is employed, with the tip of the wire being approximately 1/4 in. from the plate surface. For optimum penetration, the arc length should be such that the tip of the wire is level with or slightly lower than the surface of the plate being welded.

Fit-up of plate edges has considerable effect upon arc voltage. In laboratory practice, it was possible to butt two plates together so that a root gap of 0 in. is obtained. However, in fabrication, this type of fit-up rarely occurs. But, a gap of greater than 1/16 in. tended to allow the arc to burn through the plate and impinge on the backing bar. The weld fused to the bar, and considerable difficulty was experienced in separating the backing bar from the plate. When the arc burned through the plate or worked down the edges, the arc voltage continuously

changed. As the wire is fed by a motor governed by arc voltage, erratic arc voltage results in wire "hunting." This produced nonuniform penetration and a lumpy weld appearance.

SUMMARY

Nickel and stainless clad steels can be welded successfully by the Aircomatic process as evidenced by the excellent mechanical properties exhibited by the welds in bend tests. The Aircomatic process is superior to manual welding of clad steels in that fewer weld passes are required and the joint preparation is less expensive.

Procedures were developed for making butt welds on clad plates of $^3/_8$-in. gauge or less by the Aircomatic process. One of these procedures consists of making a complete weld by depositing a single bead of alloy weld from the backing steel side of the clad plate.

The other consisted of depositing an initial alloy weld from the backing steel side and completing the weld with one or more light beads from the clad side.

To maintain the desired ductility in welding stainless-clad steels, it was found necessary to use 25 Cr–20 Ni welding wire for all welds deposited from the backing steel side.

The ductility of welds made on type 410 clad steel plates was considerably improved by stress relieving.

Clad plates greater than $^3/_8$-in. in thickness were welded manually from the backing steel side with mild steel electrodes, while the Aircomatic process was used for the clad side. Good mechanical properties were obtained with this procedure.

Further experiments are necessary to determine the maximum plate thickness that can be successfully welded with the Aircomatic process.

In welding by the Aircomatic process, several factors are critical in obtaining sound welds of the desired penetration. The fit-up of plates must be such that a root gap of about $^1/_{32}$ in. is maintained, otherwise depth of penetration will not be controllable. The surfaces of the plates to be welded must be free of scale and rust in the vicinity of the joint to prevent porosity and arc instability.

The authors are indebted to the Air Reduction Sales Co. for their cooperation and technical assistance provided during the course of this investigation.

Welding Research Council Announces Fellowships in Welding and Closely Allied Fields

1. The Welding Research Council of The Engineering Foundation, located at 29 W. 39th St., New York 18, N. Y., is administering under its University Research Committee predoctoral research fellowships to provide young men with demonstrated marked ability, an opportunity to conduct basic research in applied sciences relating to welding and its closely allied fields. These fellowships are supported by the International Harvester Co., Chicago Bridge & Iron Co., The Linde Air Products Co., and others. Any University desiring to apply for one of these fellowships should indicate the type of problem which the university wishes to undertake, the name of the faculty adviser, and a biographical sketch and complete information in regard to candidates.

Fellows must be citizens of the United States. Training in welding, or in the fundamental sciences related to welding equivalent to at least a Bachelor's Degree in an accredited college or university, and demonstrated ability for advanced work are essential. Fellows will devote entire time to advanced study and research. Individual teaching at an appropriate level as part of his training may be arranged.

Stipends will be determined by the Fellowship Board. A maximum of $1800 for a single man, $2500 for a married man, with an added amount not to exceed $1000, may be provided annually to the institution to which the fellow is assigned for tuition, special fees, travel or necessary supplies.

2. The Plasticity Committee of the Council is creating two fellowships for predoctoral or postdoctoral work in order to stimulate original thought in the field, particularly of a type which may produce new ideas leading to either the creation or new knowledge or better utilization of existing knowledge so that brittle failures may be avoided.

Proposals will be considered from any institution in the United States or abroad. Awards will be made to two individuals, who, in the opinion of the Committee, offer the best promise of successful results in this field of study.

The basis for award will include general outstanding scientific background of the institutions for graduate study in this field, and the general scholastic attainment of the individuals. It is suggested that candidates make proposals for the beginning of their research or thought in the field in question.

Each fellowship will carry an award of $3500 to be divided between the stipend to the Fellow, tuition for postgraduate work, if any, and materials, apparatus and supplies. The exact division is not fixed, but it is expected that $3000 will cover the stipend and tuition, and $500 for materials and supplies.

Inquiries and applications should be sent to W. Spraragen, Director, Welding Research Council, 29 W. 39th St., New York 18, N. Y., and should be received not later than June 1, 1951.

Higher Design Stresses for Pressure Vessels

The effect of stress concentration in pressure vessels under conditions of multiaxial stress and for a number of life cycles of stress less than 100,000

by Perry R. Cassidy

SOME pressure-vessel designers believe that vessels designed, constructed and operated under the most favorable conditions can safely be fabricated from available high-tensile alloy steel with design stresses of $^5/_8$ to $^2/_3$ yield strength, instead of $^1/_4$ minimum specified tensile strength now permitted by A.S.M.E. Codes. Alloy-steel plate to A.S.T.M. specifications with yield point of 45,000 to 50,000 psi. is available but cannot be used economically in the construction of pressure vessels. Higher yield-point steel plate may be available up to 70,000 psi., or over.

The longest experience with pressure vessels is with those fabricated from 55,000 psi. minimum tensile-strength steel, which may be rimmed, semikilled or killed, with a design stress of 11,000 psi. There has been about 20 yr. experience with this steel using higher design stresses. Considerable experience has been accumulated with vessels of 70,000 psi. minimum tensile strength fully killed plate using 14,000 psi. design stress, and with the same steel with 17,500 psi. design stress over the last 8 yr. This later experience involves a design stress slightly under one-half yield strength, with favorable conditions of design and fabrication, but not necessarily for operation. The absence of corrosive environment in operation has not been assured.

A.S.T.M. has written a specification for high yield-strength manganese molybdenum plate for pressure vessels, A-302. Two grades, A and B, are included having minimum yield points of 45,000 and 50,000 psi. The corresponding minimum tensile strengths are 75,000 and 80,000 psi. It is anticipated by some designers that vessels can be designed, fabricated and safely operated from this steel with design stresses of from 28,000 to 30,000 psi. for Grade A and 31,000 to 33,000 psi. for Grade B under the most favorable conditions. Others may consider such stresses excessive. This should not be a matter of opinion but of proof by needed research.

What are considered favorable conditions? It is believed the following limitations represent such conditions:

1. Firebox-quality steel.
2. All forming operations hot.
3. Welds machined flush, radiographed where possible and stress relieved.
4. Not over 100,000 cycles of stress application during expected life.
5. Noncorrosive environment.
6. A reasonable restriction, developed by the proposed tests, on temperature differential, and other secondary stresses, such as at unreinforced openings, or inadequately reinforced openings, supports, head knuckles, etc.

It is recognized that in pressure vessels there will exist at certain locations concentrated stresses in limited zones that may be 2 to 2.5 times the average design stress, although it is possible by proper design to reduce this factor to 1.3 or 1.4 at reinforced openings. Under hydrostatic test of 1.5 design pressure such zones will yield as the result of plastic flow, and residual compressive stresses will develop which are balanced by residual tensile stresses in adjacent portions of the vessel. The range of stress under which such zones will operate may be about the same as the maximum tensile stress computed for the operating pressure. The stress range is not affected by the residual compression imposed, the vessel will be elastic from compression to tension, if the localized stress is not severe enough to cause further yielding on each subsequent pressure cycle.

It is known from experience that pressure vessels—boiler drums—fabricated from 55,000 psi. minimum tensile-strength steel, with 11,000 psi. average design stress, have operated over 40 yr. without failure with concentrated bending stresses in the head knuckle of twice yield point, or from yield point in compression to yield point in tension, on the outer fiber, for relatively few life cycles.

Is it not logical to conclude that for higher strength steel with design involving reduced stress concentration factors, vessels can be safely designed with higher average stresses if care is used in minimizing the value and extent of maximum secondary stresses?

Perry R. Cassidy is Chairman of the Pressure Vessel Research Committee, Welding Research Council.

The Pressure Vessel Research Committee proposes to investigate this question. It has prepared a research program in three parts.

First, to test flat plate specimens under biaxial strains in the central portion under fatigue pulsations. Maximum bending stresses will be imposed that will produce failure in from 10,000 to 200,000 cycles. Two steels will be used, A 201 Grade A and A 302 Grade B. The effect of welds and stress-raisers consisting of mild and sharp grooves as well as strain gradient will be determined.

Second, to correlate the flat plate tests with "model vessel" tests under one similar set of conditions.

Third, to construct and fatigue test two full-scale pressure vessels from each type of steel, of identical dimensions, containing reinforced openings and other normal stress raisers.

In the first step, exploratory work has been in progress for a year and is still under way at the Research and Development Laboratory of the Babcock & Wilcox Co. at Alliance, Ohio, on $^3/_4$-in. thick flat plate specimens under repeated hydrostatic pulsations. The distribution and amount of strain in the specimens have been measured by strain gages. The shape and dimensions of the specimens selected have been found to produce biaxial stress in the center with a ratio of two to one.

After this preliminary study is completed to the point where it is reasonably assured that fatigue testing in this manner will produce useful information that may be equivalent to that from fatigue tests of model pressure vessels with welding and stress-raisers, the Pressure Vessel Research Committee plans to arrange for a project at a University Laboratory to construct a biaxial fatigue testing machine and carry out a program of tests as outlined in step one.

If the project under step one proves conclusive and indicates that high average stresses in high-tensile steel with stress concentrations of typical kinds does not result in premature failure as compared with medium tensile steel, steps two and three will be initiated. It is expected that step three will be a cooperative project with the full-scale vessels built and tested by contributing organizations without cost to the Pressure Vessel Research Committee.

A pressure vessel which is expected to have a useful life of 40 yr. will have an expected number of life cycles as follows for the indicated time between cycles of stress application:

	Cycles
1 week	2,080
1 day	14,600
6 hr.	58,400
4 hr.	87,600
2 hr.	175,200

Experience in uniaxial fatigue testing, and in a very limited amount of biaxial tests, indicates that the stress for failure is inversely proportional to approximately the 0.18 power of the number of cycles for failure. Whatever the slope of the *S-N* curves in the proposed biaxial test may be found to be, it would seem reasonable that a factor of safety of 10 based on the cycles to failure with the maximum expected concentrated stress present should represent a safe basis of design. For an exponent of 0.18 this would correspond approximately to a factory of safety on stress of 1.5.

It may be questioned whether there can be assurance that maximum stress concentration in a pressure vessel can be considered to be less than that resulting from a sharp crack in the material, with unknown size and orientation, irrespective of favorable design to reduce stress concentration at openings, points of support, etc. The use of the best available steels, careful inspection for defects and the use of radiography on all welded joints will reduce the probability of such severe stress-raisers. The proposed tests will indicate the relative susceptibility of the medium and high-tensile steels to various degrees of severity of stress-raisers for cycles to failure between 10,000 and 200,000. It may be found that in this range the severity of stress-raisers has less effect in reducing the stress for failure than is the case for a life between 200,000 and 2,000,000 cycles.

Fatigue tests at the University of Illinois have indicated that welded plate with the weld reinforcement machined flush have about the same fatigue strength as the base plate. With serious defects in welds eliminated by radiographic examination, it is not anticipated that welding will be a major factor, in effecting the fatigue strength, in the absence of harmful metallurgical effects.

It is recognized that experience over years of service is the best guide to the designer of pressure vessels, but in new untried fields it is the function of the engineer to use theoretical methods and test procedure to anticipate with reasonable assurance what long years of service experience will confirm.

Stress Distribution in Plastic Range in a Rigid Frame

Studies of the action of a fully continuous frame welded for full strength of the material under the influence of a few repeated loads carried well beyond the elastic limit

by L. E. Grinter, Charles Peller and John Butkus

IN THE fall of 1940, the senior author outlined the tests now reported upon 10 yr. later. The welded-steel test frame to be described was manufactured in 1941 by the Chicago Bridge and Iron Co., and was stress relieved by the Lasker Boiler and Engineering Co. Only the initial tests could be completed before war began and it was 1948 before the project was again reactivated. Final overload tests were made in 1949 and are reported here. The early tests for loads below the elastic limit were conducted by Leonard Zick and Walter Pierce. Tests for loads above the elastic limit were conducted by Charles Peller and John Butkus.

The objective of the tests reported was to determine the action of a fully continuous frame under the influence of a few repeated loads carried well beyond the elastic limit. The frame was stress relieved by annealing to eliminate or greatly reduce residual stresses with the hope that some study of residual stresses to be introduced by plastic deformation could be made. The frame was welded for full strength of the material. The evidence of the tests is that the welding was wholly effective since no distress was observed in any weld.

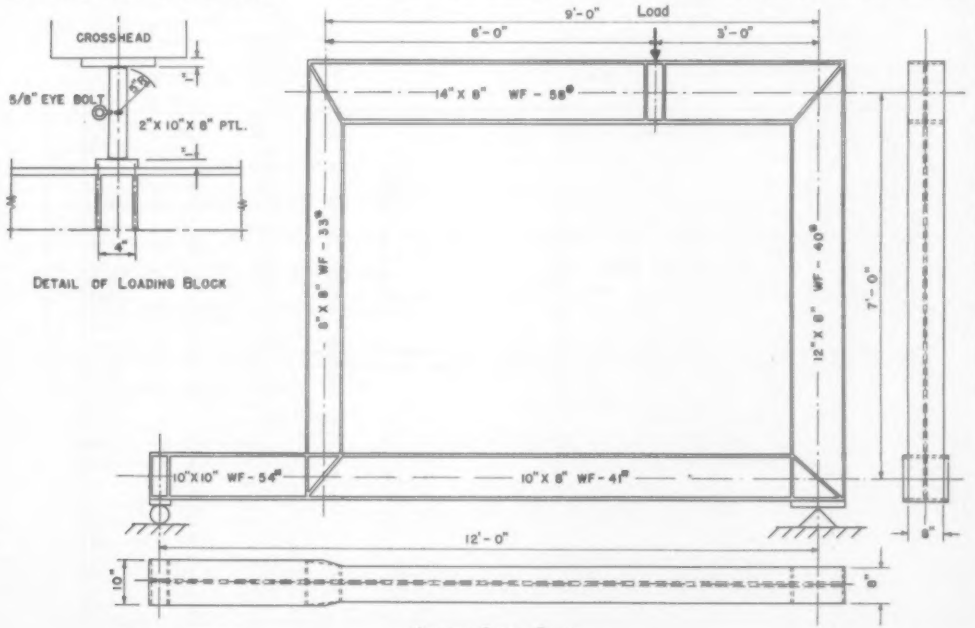
L. E. Grinter and John Butkus are connected with the Depts. of Civil Eng. and Mechanics, Illinois Institute of Technology, Chicago, Ill.

Charles Peller is with Valparaiso University, Civil Engineering Dept., Valparaiso, Ind.

Presented at the Thirty-first Annual Meeting, A.W.S., Chicago, Ill., week of Oct. 22, 1950.

THE TEST SPECIMEN

The design of the frame shown in Fig. 1



WELDED STEEL FRAME

Fig. 1

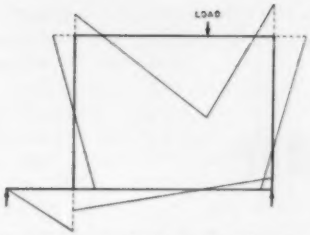


Fig. 2 Theoretical moment diagram

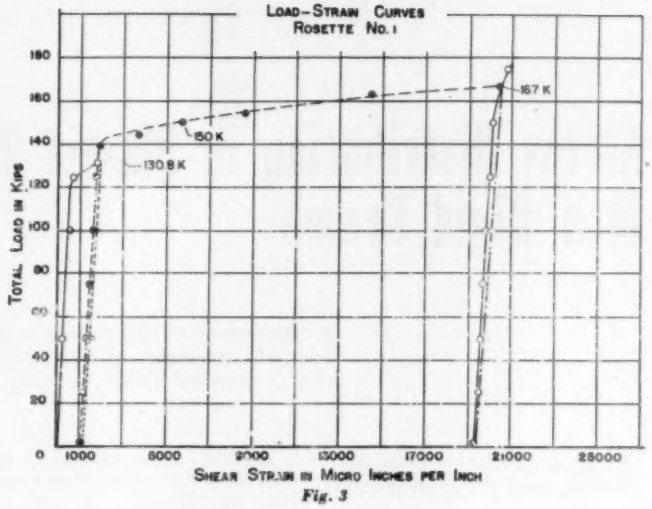


Fig. 3

had several objectives. First, within the limits set by the clearance of the available 400,000-lb. mechanical testing machine it was desired to make the frame as near full scale as possible. Actually, the result is a frame of 7-ft. height and 12-ft. span (Fig. 1) which is at least one-half the scale of a normal panel in a building. By using a single concentrated load applied through a pair of vertical stiffeners and a cantilever projection at the left the sections required were normal structural beam sections ranging up to the 14 x 8—58-lb. wide-flange beam. With such dimensions and sections, it is felt that the test may be looked upon as a study of a structural frame, not a model. The second purpose of the cantilever at the left side of the frame, which may be seen in Fig. 1, was to control the moment diagram. The moment diagram of the top member (Fig. 2) is that of a normal restrained beam with positive moment under the concentrated load and negative moment at each end connection to a vertical post. The moment diagram of the left-hand column is typical of wind moments in a building frame, i.e., there is a point of contraflexure almost exactly at the mid-height. The lower member and the right-hand post are similar in that each has a small moment at the lower right-hand corner and a larger moment at its opposite end. Thus, in this one-frame test several kinds of structural action were observed.

LOADING AND MEASUREMENT

The only load applied to the frame was

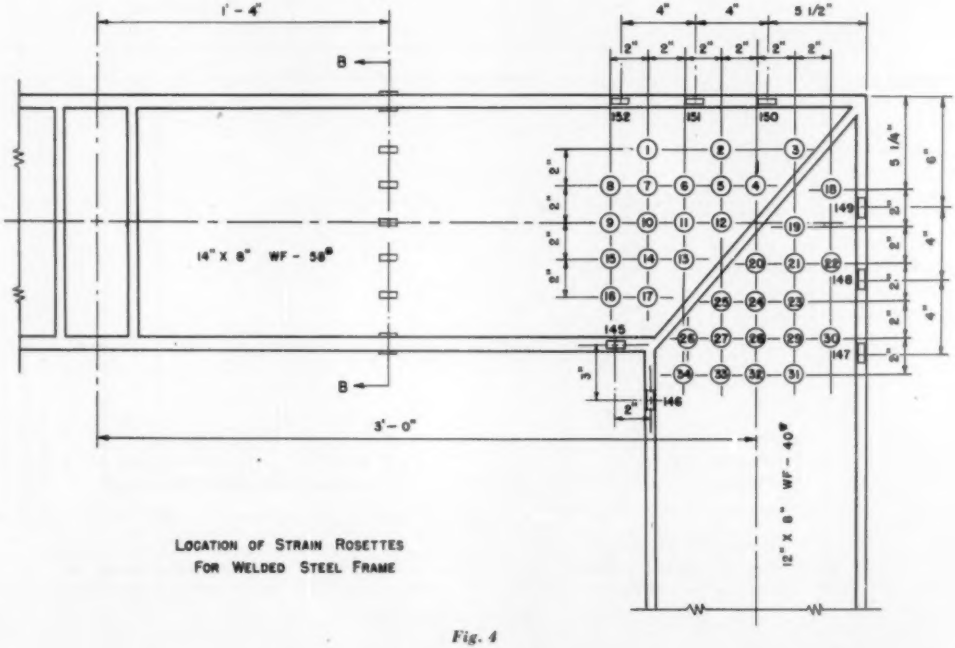


Fig. 4

a vertical concentration at the quarter point of the 12-ft. span over the vertical stiffeners of the top member. In the initial elastic stages the load was applied by means of the mechanical-type machine in increments of a few thousand pounds up to the design load of 100,000 lb. At this load the calculated maximum beam stress (under the load) was 20,800 psi. and the calculated maximum column stress was 20,300 psi. Since it was believed that failure would be initiated by local yielding and local buckling, no major distinction was made between beams and columns of the frame in regard to design stress. If the loading had been of the gravity type, a different viewpoint might have prevailed in this regard.

After convincing data had been taken up to the design loading to establish the elastic action of the frame, the load was increased at normal machine speed to 145 kips. Flow took place and the load finally stabilized at 130.8 kips which is plotted against the first permanent set in Fig. 3. This load strain curve for rosette No. 1 is reasonably typical of those for a large number of electric strain gages placed on both webs and flanges, 367 gages being used for this purpose. The locations of these gages and some of the recorded data will be given later. Of course, many of the gages were placed on elastic portions of the frame and gave elastic load-strain curves even at the highest loads. These gages served to determine the distribution of moment even when a few gages became inactive or erratic because of excessive deformation. In the early tests for low loads Berry gages were used to check the moment diagram in detail but full dependence was placed upon SR-4 gages in the over-load tests because of the time element involved.

After a small permanent set had taken place at 130.8 kips of load, the load was released in 25 kip steps which shows as an elastic recovery line (solid dots in Fig. 3). The load was then reapplied with elastic results up to at least 130 kips and then with plastic deformation up to 167 kips. However, Fig. 3 does not clarify the fact that each load point plotted represents not the maximum load reached for that particular application but the load after the drop-off had occurred. For example, the point plotted at the load of 150 kips (Run 16) really represents the following sequence of events. The load was increased at normal speed of machine operation from the previous load of 145 kips to a maximum value of 156 kips. The load then started to drop rather rapidly as plastic flow occurred and in 10 minutes it had dropped from 156 to 151 kips. The drop-off had then become so slow that the gages could be read. During this period of nearly an hour the load dropped only another kip to 150 kips which the structure appeared to sustain. On Fig. 3 one gage reading is plotted against this relatively stable load of 150 kips. At this load the vertical set in the upper beam was $^3/_8$ in.

We see then that although the load was reduced to zero and reapplied only twice a load drop-off of from 5000 to 10,000 lb. occurred some ten times with consequent strain hardening. No other method of collecting extensive load-strain data for loads above the yield point appears feasible. The final repetition of load followed a reduction from 167 kips to zero as shown on Fig. 3. At this point the permanent set in the upper beam was 1.55 in. After a perfectly elastic return to 150 kips the load was increased to a final value of 175 kips after which dials were removed and the recording of measurements was terminated. The plastic deformation under the load increased from 1.55 to 2.1 in. while this load was maintained for a period of about 45 min.

Inspection of the frame during this final stage showed visible local buckling of the upper flange of the top beam near the load; a visible angle change of about 3° of the joint nearest the load, some of which seemed to occur near but outside of the joint, and a local distortion short of buckling visible in the flange of each column. On the basis of this inspection, it was concluded that the frame had passed its maximum useful loading and that the test should be discontinued.

The measurements taken included flange strains for evaluating bending moment at

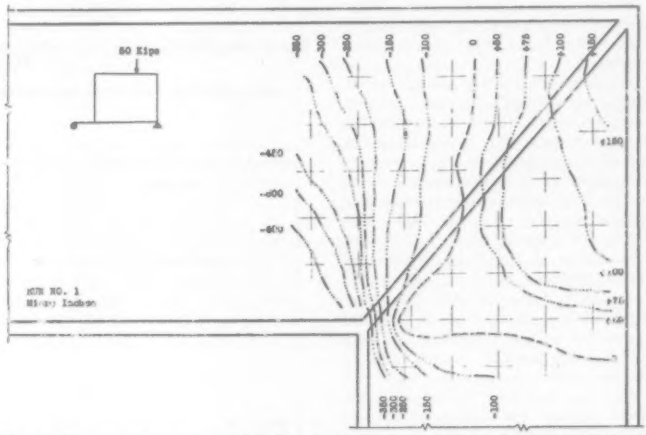


Fig. 5 Shear strains at knee of welded steel frame under a load of 50 kips (initial loading)

Strains plotted are from initial zero readings

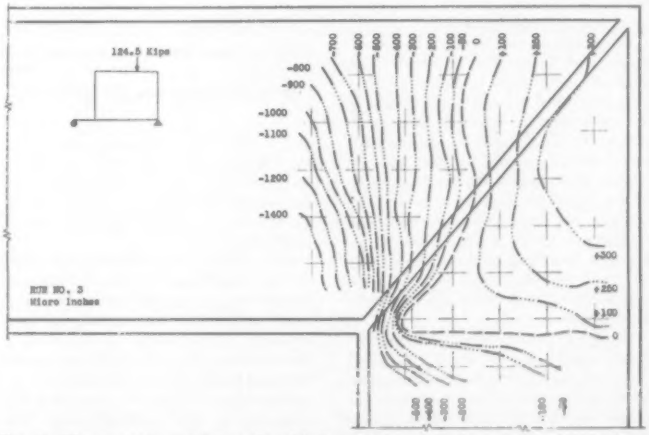


Fig. 6 Shear strains at knee of welded steel frame under a load of 124.5 kips (initial loading)

Strains plotted are from initial zero readings

several sections and web rosettes primarily at one section near the load and at the corner adjacent to the load where flow was expected and was obtained. In the main the gages appeared to operate reliably. Those on cross sections that were far from the load were not influenced by excessive deformations and only a few gages in areas of large deformation became inoperative or obviously unreliable. Deflections were measured by dial gages attached to a base-line frame swung from pins through the corners of the test specimen. True flexural deflections rather than displacements were therefore measured directly.

QUESTIONS AND ANSWERS

Some of the questions raised in the minds of the designers and the qualitative answers furnished by the tests are as follows:

Would the use of a 1-in. plate above the stiffeners and a roller of 10-in. diameter permit sufficient freedom of sidesway for the test frame? The correspondence of moment diagrams for the elastic action of the frame with theoretical analysis shown in Fig. 13 indicates that reasonable freedom of sidelurch existed at least within the elastic range. Figure 19 shows the sidesway for load applications that produced plastic flow.

Would welded joints with $^1/_2$-in. diagonal stiffeners function to produce full continuity around the corner? Under elastic action the tests indicate full continuity. After plastic action began the evidence is less clear. Some permanent angle change was eventually reached (about 3°) between the top horizontal member and the right-hand post (see Fig. 20). However, as will be shown later, the moment diagrams obtained, even after excessive deformations, were indicative of reasonably satisfactory although not of continuous structural action.

There were questions as to the elastic stability of the vertical members which were adequately fixed in position at their bases but were laterally unsupported at the upper corners. The only lateral support at the top being at the concentrated load. At maximum load the adjacent column was stressed in direct compression to 11,100 psi. plus a very severe flexural moment which brought its maximum fiber stress up to the yield point. Its L/r for a 10-ft. length from its base to the load was 62. It finally showed some local buckling of a flange but no severe over-all distress.

The question was raised as to whether several applications of a load increased with each application to produce additional flow would induce fracture either in base metal, in the weld zone or in the transition zone. For this structure which had been stress relieved, there was no evidence of any fracture or even of approaching fracture after three load repetitions from zero load into the plastic range and numerous replacements of 5000 to 10,000 lb. of load that had dropped off due to plastic flow.

There was the question as to how effective the plastic redistribution of moment would be in protecting such a structure from distress while maintaining its load capacity. The evidence of the tests is that the capacity of the structure to sustain load elastically was certainly not reduced but for the type of loading used was increased by the overload tests. For similar loading the final distorted structure with deformations in excess of any that might be permitted in practice (1.55 in. of permanent deflection in a span of 9 ft.) seemed to react elastically for a load 20% larger than the load at initial observed flow.

Finally there was the question as to whether a welded frame would strain harden in the same way that a tensile bar increases in elastic limit by overstraining. The evidence of the tests is that strain hardening occurred since the elastic limit of material that ranged from 25,500 to 30,000 psi. before the frame was manufactured increased to 31,900 to 37,400 psi. for the samples cut from the distorted frame. It is not clear however to what extent this strain hardening effectively strengthened the corner joint.

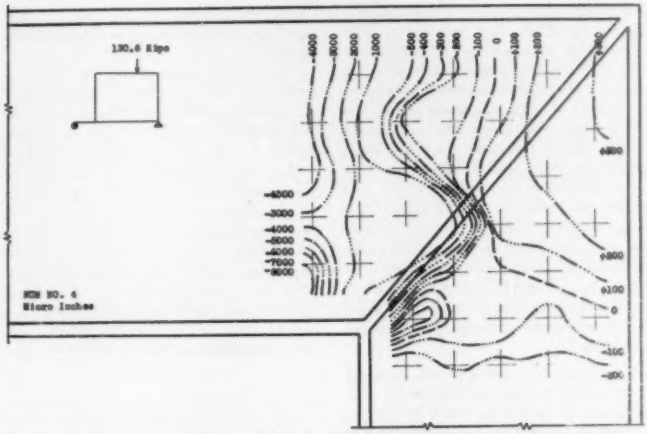


Fig. 7 Shear strains at knee of welded steel frame under a load of 130.8 kips (initial set)

Strains plotted are from initial zero readings

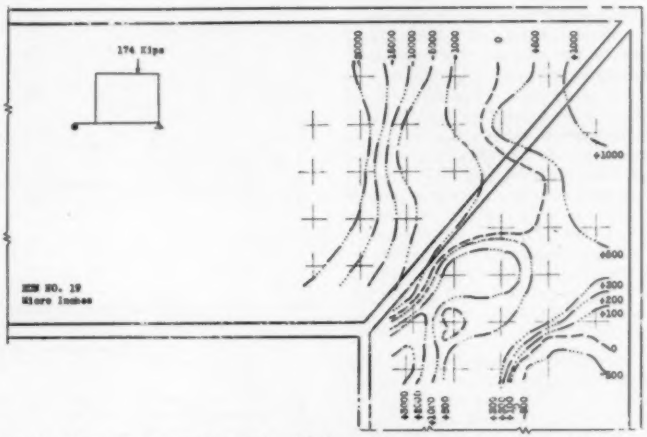


Fig. 8 Shear strains at knee of welded steel frame under a load of 174 kips (second set)

Strains plotted are from new zero readings after load release from initial set

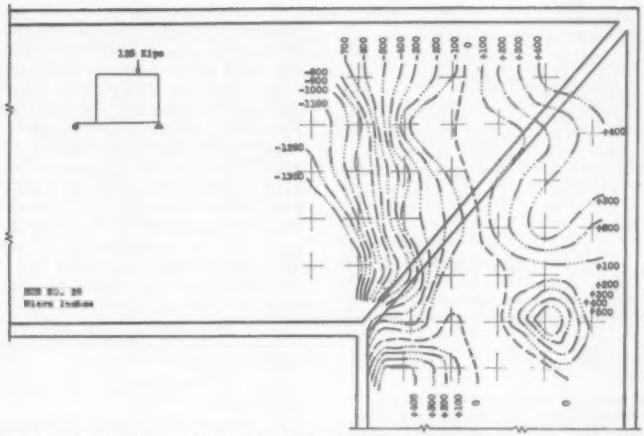


Fig. 9 Shear strains at knee of welded steel frame under a load of 125 kips (load application a third time)

Strains plotted are from new zero readings after load release from second set

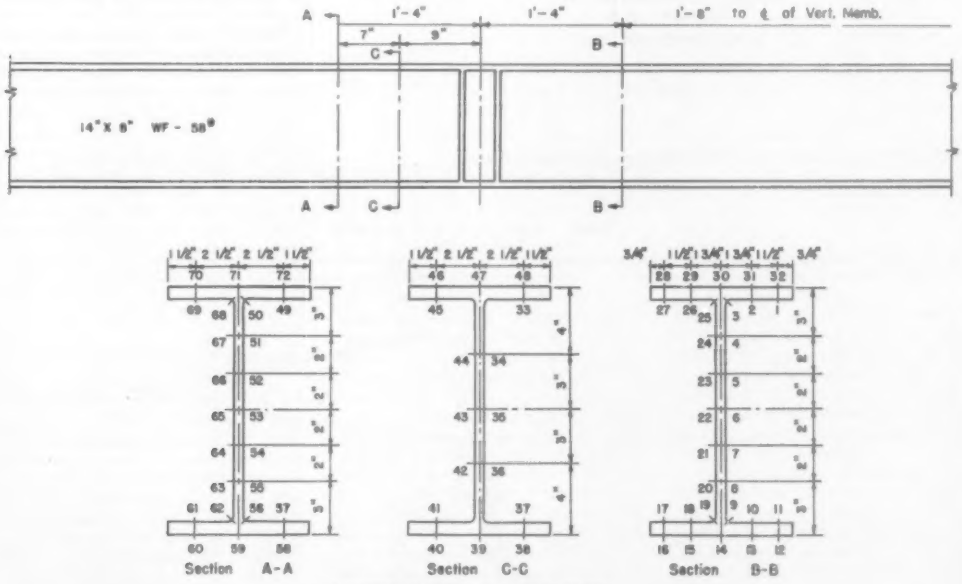
STRAINS AT THE KNEE

In Fig. 4 is shown a diagram locating the strain rosettes placed at the knee of the frame nearest the load. Opposite faces of the same knee carried identical rosettes so that local buckling could be studied by comparing the measurements on opposite faces of the web. Axial strains were measured by the rosette and from these data the magnitudes and directions of the principal strains as well as the shear strains were computed.

The reader will observe in Figs. 5 and 6 a definite regular pattern of the shear strains as determined for loads of 50 kips and 124.5 kips. This regularity is in contrast to the irregularity of shear-strain distribution at higher loads. As the load increased from 50 to 124.5 kips the zero strain contour shifted slightly toward the inner portion of the knee and the intensities of the shears increased approximately in proportion to the load. It is observed that the positive shear strains which lie to the right and outside of the zero contour are small in comparison with the negative shear strains (one-third to one-fourth in magnitude). The curves of Figs. 5 and 6 indicate clearly that the critical area of the web is close to the inside flange of the knee.

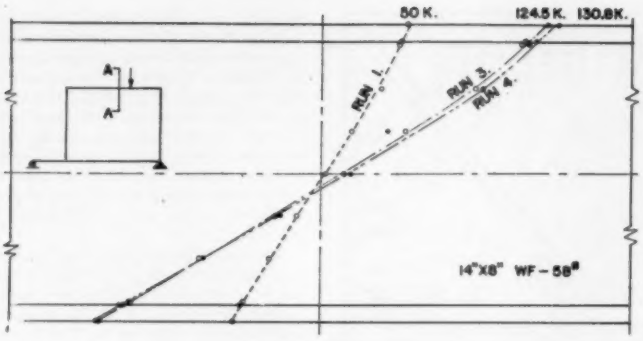
At a load of 130.8 kips (see Fig. 7) which was sufficient to put an initial set in the frame, there was slipping of web metal around the lower end of the diagonal stiffener. By comparing Fig. 7 for a load of 130.8 kips with Fig. 6 for a load of 124.5 kips the great change in pattern observed makes it abundantly clear that the yield point had been passed for the metal in the knee. If this had not been so, Fig. 7 would be essentially a duplicate of Fig. 6 since the loads were nearly the same.

The next illustration of shear strains, Fig. 8, is for the first application of a load of 174 kips (approximately 74% above design capacity). The frame was flowing gradually while these data were being taken since the frame never developed capacity to sustain a load of this magnitude without yielding. A great change in pattern of shear strains is evident in Fig. 8. Maximum strains are three times those measured for 130.8 kips of load and 15 times the elastic strains measured for a load of 124.5 kips. Very severe flow had unquestionably taken place.

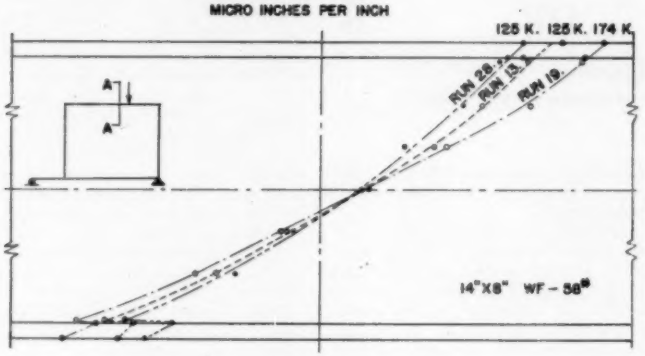


LOCATION OF ONE INCH STRAIN GAGES
Top Horizontal Member of Welded Frame

Fig. 10



STRAIN DISTRIBUTION AT SECTION A A
MICRO INCHES PER INCH



STRAIN DISTRIBUTION AT SECTION A A
MICRO INCHES PER INCH
ACCUMULATED STRAINS FROM INITIAL ZERO READING

Fig. 11

The illustration, Fig. 9, is for a final loading of 125 kips. The strain distribution may be compared with Fig. 6 since the frame acted elastically within this range. It is seen that even the severe deformation caused by the preceding 174-kip load did not greatly change the capacity of the knee to act elastically for a load 20% above the initial limit of elasticity and 50% above the design loading. (The effects of knee distortion upon the moment diagram and the deflection diagram will be discussed later.) The larger strains which occur to the left of the stiffener agree reasonably well on Figs. 6 and 9. The same general correspondence of elastic strains before and after overloading was found for the maximum principal strains.

From the study of strains at the knee it appears that

1. Stresses and strains in the outer corner of the knee are small, and it does not seem possible to make this material effective.
2. The diagonal stiffener clearly served a very useful purpose since web buckling should otherwise have been initiated by the shear slip observed in the knee.
3. The diagonal stiffener did not prevent shear slip which resulted in some angle change between column and girder.

It would be well for investigators to consider whether another arrangement of stiffeners or of other reinforcement might not perform better in this regard. Stress distributors rather than a web stiffener might be given consideration.

TOP HORIZONTAL MEMBER

Figure 10 shows the location relative to the load point of three cross sections of the upper horizontal beam that were studied. Section A-A which was 1 ft.

Strains plotted are from new zero readings after load removal from initial set

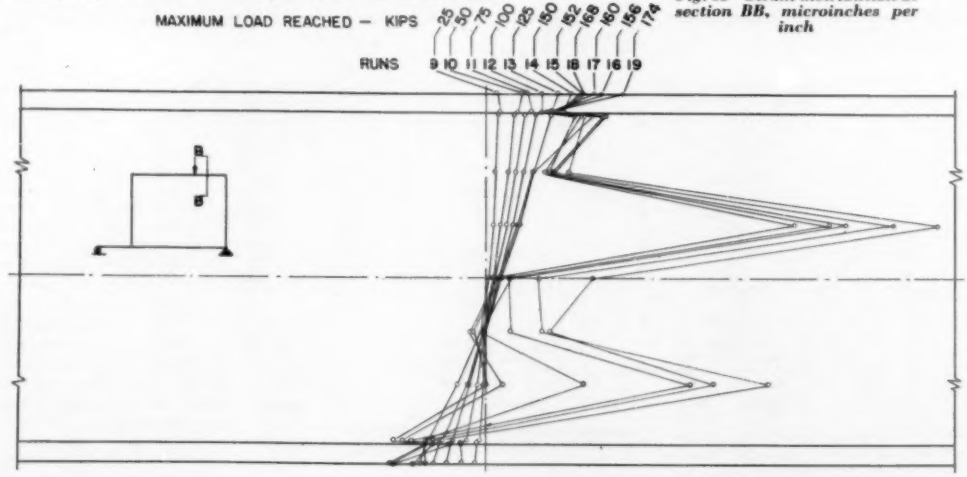


Fig. 12 Strain distribution at section BB, microinches per inch

4 in. away from the load point toward the smaller reaction acted in a rather normal or anticipated manner throughout the loading. Actually the maximum load of 174 kips would theoretically produce fiber stresses at Section A-A of only 27,500 psi. which is approximately the minimum elastic limit observed in coupon tests. Hence, the distribution of Fig. 11 may be considered to be for elastic material at the Section A-A even though the frame was yielding elsewhere.

It is in Fig. 12 that the erratic action for a cross section influenced locally by plastic flow may be observed. This figure refers to cross-section B-B nearly halfway from the load to the more heavily stressed knee. The flanges here were not heavily stressed since B-B is not far from the theoretical point of contraflexure or of zero moment. The web however was under heavy shear and B-B is within 14 in. in each direction from web material that underwent severe plastic flow. Hence, it is not surprising that some shear strain adjustment took place at B-B. Since strains were measured on opposite faces of the web and since these measurements checked each other, buckling and slippage of strain gages were both eliminated as possible explanations. Local slippage of the metal had clearly taken place probably as a part of the over-all adjustment necessitated by the flow at the load and in the knee.

The erratic readings at B-B were observed as soon as the load passed 130 kips and a small permanent set occurred. But each time the load was reduced to zero and reapplied a regular set of strains were measured for section B-B until the load became large enough to increase the permanent set in the frame. In Fig. 12 we see such a set of results. For loads 9 through 13 (25 to 125 kips) the strain distribution at B-B is approximately linear, but for a load of 150 kips which produced a new permanent set the erratic strains plotted were recorded by the gages. The fact that these larger strains are compressive both above and below the mid-height of the beam clarifies the fact that they are due to regional shear slip and not bending moment.

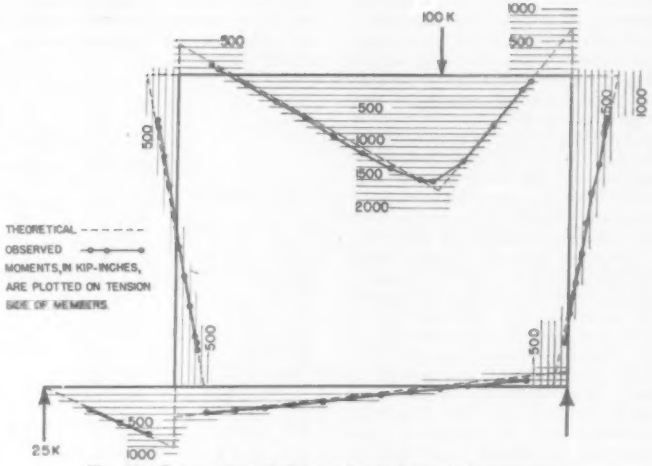


Fig. 13 Comparison of observed and theoretical moments

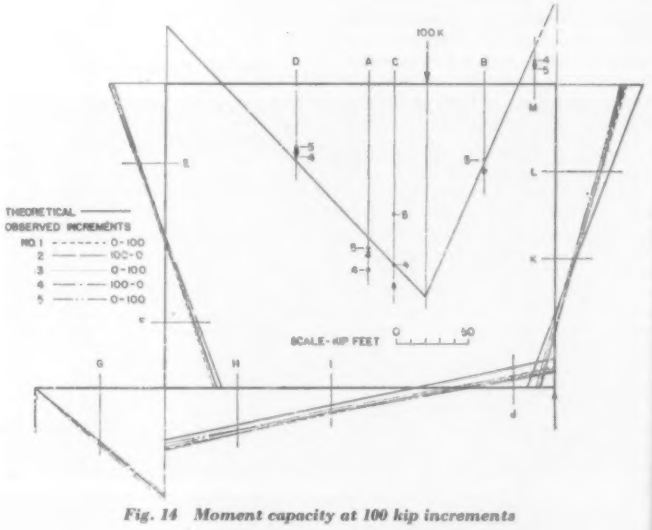


Fig. 14 Moment capacity at 100 kip increments

MOMENT DISTRIBUTION

For early tests in the elastic range the moment distribution was determined from Berry strain-gage readings. Three measurements were made at each section on each flange, and average strains were used for moment calculation. The theoretical moment diagram shown by broken lines in Fig. 13 is in close agreement with the plotted points obtained from measured strains for the design loading of 100 kips. Actually, the measured strains are increments from a load of 20 kips to a load of 120 kips which eliminated the inaccuracy of zero gage readings. Some rounding of the moment diagram under the load was produced by the distribution of load through the bearing plate and the vertical web stiffeners.

Strain measurements beyond the elastic range were made with electrical strain gages of the SR-4 type. The sections studied are marked by letters *A* to *M* in Fig. 14. Five loadings of 100-kip range are illustrated on Fig. 14. Number 1 is an increment from zero to 100 kips which was then increased to 130.8 kips to produce plastic flow. Number 2 is the recovery from 100 kips back to zero. Number 3 is a second increment of load from zero to 100 kips the load then being increased to 167 kips (174 kips before drop-off) which produced a permanent deflection under the load of 1.55 in. Number 4 is the recovery as the load is backed down from 100 kips to zero. Number 5 is the final application of load from zero to 100 kips which was then increased to a maximum load of 175 kips.

A study of Fig. 14 indicates smaller moments than the theoretical moments for both verticals. The lower horizontal member therefore has an increased moment at the left (controlled by the cantilever reaction) and a decreased moment at the right.

These members act elastically at all loadings but they do have zero-load

moments which are the residual moments produced in the frame by plastic flow under the load and at the upper right-hand corner. However, since the plotted lines of Fig. 14 are obtained as moment increments (or decrements) for a load range of 100 kips, this residual effect should be largely eliminated. The reduction of moment in the right-hand vertical is therefore real and can be explained only by failure of the upper right-hand corner to transfer moment into the vertical after the corner had begun to flow plastically.

The upper horizontal member which underwent plastic flow near the load and at its right-hand end provided a less consistent set of moments. It seemed clearer to show plotted points here since there is always some question as to the satisfactory action of SR-4 gages near sections of known plastic flow. The consistency of the grouped points at section *M* near the right-hand corner indicates general agreement with the reduced corner moment as obtained by measurements on the right-hand vertical and further confirms the fact that plastic flow within the knee had reduced the transfer of moment at the corner. The other plotted points for the horizontal beam appear to involve major inconsistencies which may be due to the influence of gage slip under plastic flow.

DEFLECTIONS

The magnitudes of observed and theoretical deflections for an initial elastic loading of 100 kips are shown in Fig. 15. The theoretical deflection of the upper member is subdivided into moment and shear effects. Sidesway is included and the observed deflections check theoretical values except for the lower member. This is caused by an excessive rotation of the lower left-hand joint which is unexplained.

In Figs. 16–18 the deflections of three members are shown for each return to a load of 100 kips as the load was increased and decreased. The several curves of Fig. 16 for the bottom horizontal member are reasonably consistent and are also in reasonable agreement with the corresponding elastic deflection curve of Fig. 15. The deflection curves of Figs. 17 and 18 should be studied in two groups. The three small deflection curves on each figure are consistently analyzed as follows: The dotted lines are for an initial load application and they agree in general with the original elastic deflections of Fig. 15. Then after some plastic flow due to overload of 130.8 kips the deflection obtained after backing down to 100 kips is shown by the line of short dashes. Note that these deflections for the beam of Fig. 17 are about 200% larger than the elastic deflections but they are much smaller than the elastic deflections for the right-hand column. The conclusion from this evidence is again that the upper right-hand corner was deformed by overload and did not transfer moment and rotation around the corner into the right-hand column with full effectiveness.

Further evidence of joint distress is gained from study of the pair of large deflection curves on Figs. 17 and 18. On Fig. 17 the slope at the right-hand end of

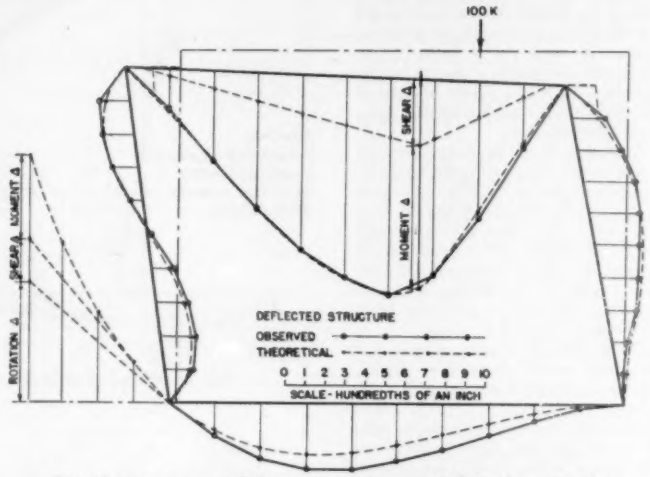


Fig. 15 Comparison of observed and theoretical deflections, elastic

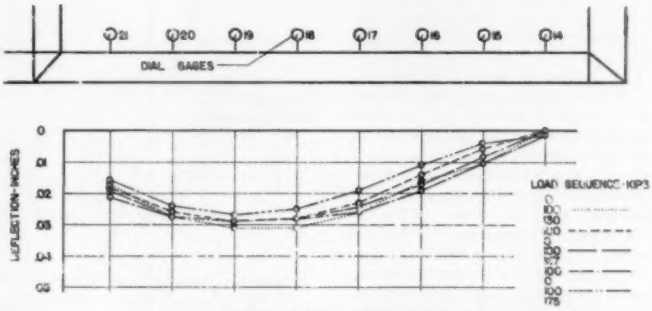


Fig. 16 Deflection of bottom horizontal member

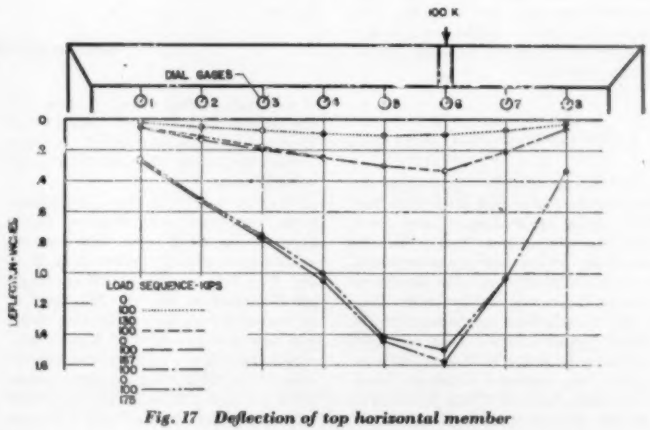


Fig. 17 Deflection of top horizontal member

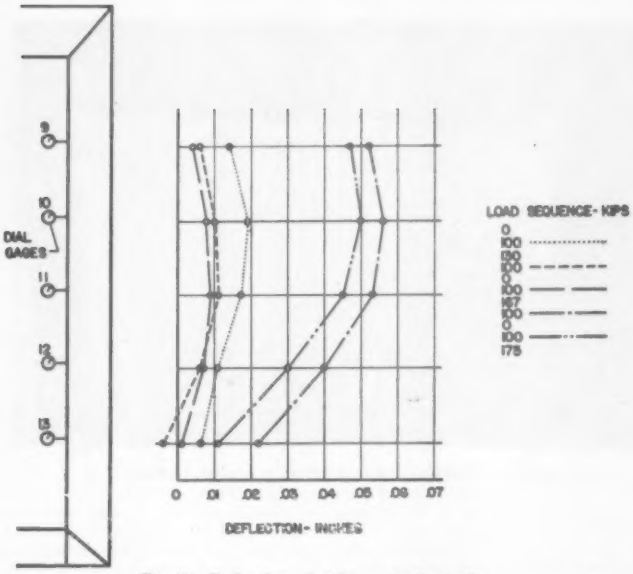


Fig. 18 Deflection of right vertical member

moments by plotting differences or changes of moment. This was not done with measured deflections which represent absolute movements rather than increments of deflection due to the 100-kip

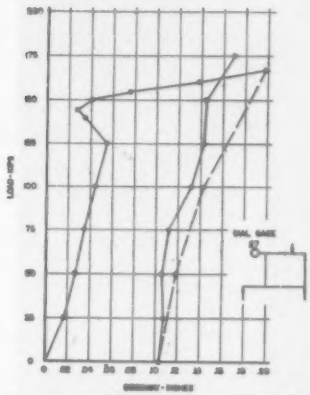
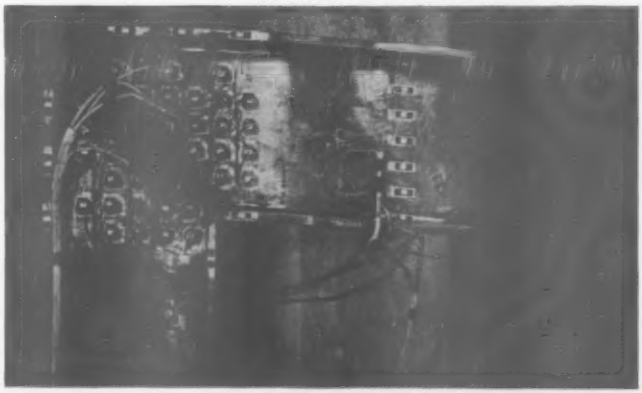


Fig. 19 Sidesway pattern after initial yielding

Fig. 20 (Below) Final distortion at right-hand knee of Fig. 1

the horizontal beam continually increases with repeated overload reaching ultimately perhaps 10 or more times the initial elastic rotation. In comparison, the upper end rotation of the right-hand vertical member increases to no more than 4 times the elastic rotation. These rough estimates are based upon the plotted values for gages 8 and 9 on Figs. 17 and 18. They present further evidence of the gradual collapse of the corner and the formation there of a plastic hinge of no greater moment capacity than the initial elastic resistance of the corner.

The fact that the next to the last set of deflections in Figs. 17 and 18 are larger than the last set is associated with residual deflections introduced by plastic flow. This effect was eliminated from measured



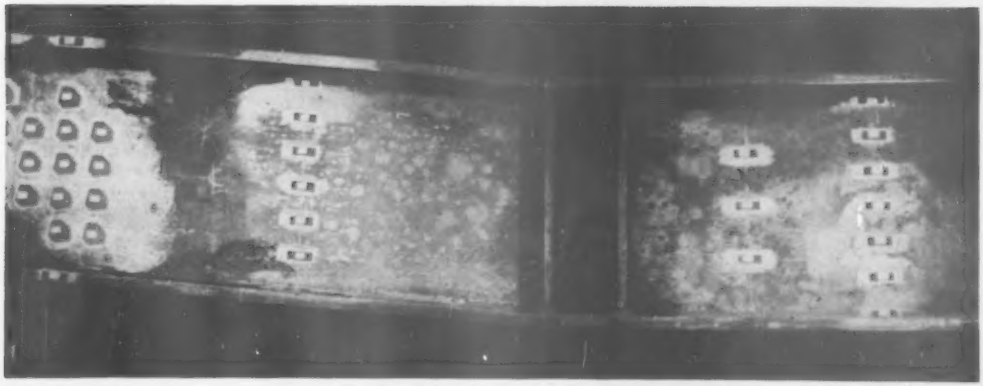


Fig. 21 Final distortion at load point of Fig. 1

Fig. 22 Final distortion of left-hand column of Fig. 1

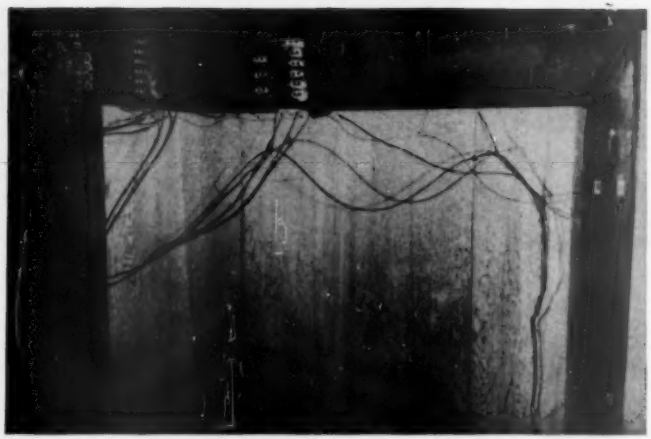


Fig. 23 Final distortion of frame, rear view from Fig. 1

load. Since the base frame for deflection measurement was attached to the corners of the structural frame by pin connections, it followed sidelurch of the structural frame and eliminated the movements due to sidesway from all measurements. Sidesway as shown in Fig. 19 was a bit erratic which was to be anticipated since sidesway motion requires an overcoming of friction and of any other resistance that occurs at the load point. The loss of sidesway following the second overload (above 130.8 kips) which is apparent on Fig. 19 appears to be associated with the development of plastic flow under the load and particularly at the right-hand corner since transfer of moment into the column is necessary to produce sidelurch.

PHOTOGRAPHS

The view of the corner, Fig. 20, show the application of the electric strain gages on the web and something of the corner distortion.

The view of the load point and its stiffeners, Fig. 21, shows the heavy vertical deflection (over 2 in.) and the beginning of a buckle in the upper flange.

The view of the left-hand column in Fig. 22 shows the start of a buckle of the inside flange.

The final photograph, Fig. 23, is a general view of the frame after the tests were completed. Even in this distorted condition the frame maintained its capacity to withstand a load of 175 kips while plastic flow continued in a gradual manner.

CONCLUSIONS

1. In general, the test frame showed capacity to resist a final overload of 75% without actual fracture or buckling collapse. Nevertheless, all overloads above 25% produced plastic flow.

2. The frame strain hardened as is indicated by individual strain-gage readings and by elastic recovery of deflection for repetition of loads as high as 20% above the load that produced initial plastic flow.

3. The action of the corner joint near the load is important. This joint with diagonal stiffener has proved to have excellent resistance to moment in model knee tests. Its action in the structural frame was not so laudable. There is clear evidence that flow occurred in the joint at a load of 130.8 kips. From then on the joint was relieved of some 20% of its theoretical moment. It seems clear that a redesign with inside corner reinforcement could be devised to delay development of the plastic hinge at the corner and thus increase useful load capacity.

4. The unique test frame used in these experiments proved to be entirely satisfactory. It might properly be used for a more extensive set of tests for investigating corner or knee reinforcement.

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"PHYSICAL AND MECHANICAL CHARACTERISTICS OF MATERIALS FOR PRESSURE VESSELS"

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Stress Relieving and Fracture Strength

Preliminary studies to demonstrate that residual stresses are an important factor in the failure of welded structures under conditions which inhibit plastic flow

by D. Swan, A. R. Lytle and C. R. McKinsey

Abstract

The factors of straining and heating involved in the application of the controlled low-temperature stress-relieving process have been investigated by studying their effect on transition temperature. From the results of these tests and a series of mechanical tests, it was concluded that there was no appreciable metallurgical effect from the heating and straining accompanying this process.

It has been proposed that residual stresses may be quite important in the failure of welded structures under conditions which inhibit plastic flow. The amount of plastic deformation occurring during a destructive test, as measured by the amount of strain hardening which takes place at the initiation of fracture, was shown to be dependent upon severity of testing conditions. Available data were analyzed, and conditions necessary for the study of the effect of residual stresses were outlined. A few preliminary tests were made, and the results were compatible with the hypothesis, although further tests are necessary to confirm its validity.

INTRODUCTION

THE effect of "locked-in" stresses, introduced into a structure by welding, has been an intriguing and highly discussed subject for years. It is generally accepted that residual stresses of high magnitude are normally present in a weldment, but the importance of these stresses and their effect on the service behavior of the weldment have been debated to a considerable extent. It is indeed a complex problem, and it seems apparent that no clear-cut complete answer has yet been given.

It is a relatively simple matter to relieve residual stresses. Furnace stress relieving, mechanical and hydrostatic loading and low-temperature stress relieving are commonly used methods, and long experience with such operations have confirmed their value. It is pertinent to this investigation to note that for this purpose the mechanical methods of stress relief have given as good results as the thermal methods. In spite of the widespread use of stress-relieving processes, there has been uncertainty as to whether any benefit was directly and solely attributable to the relief of stress, as the improvement in performance of furnace stress-relieved structures—the most widely used method—has been attributed chiefly to metallurgical changes. The possible benefits resulting from the reduced stress level have consequently been minimized.[1,2,3] It is very important, however, to the welding and fabricating industry to determine the actual benefit to be gained solely from a stress-free condition. Many structures cannot be furnace treated, and some steels are adversely affected by the temperature of stress relieving. For such assemblies, the assurance that the elimination of stress in itself is of primary importance and that the thermal treatment is unnecessary, would be a great help because it would permit taking advantage of the mechanical methods of stress relief, such as hydraulic testing and controlled low-temperature stress relief. This investigation was undertaken to determine the conditions necessary for the study of residual stress and the general effects of stress relief on large welded structures. It should be emphasized that this preliminary study is concerned only with the effects of residual stress as differentiated from the metallurgical effects to be derived from the 1100–1200° F. postheat.

REVIEW OF PREVIOUS WORK

Relatively few investigations have been directed primarily at the determination of the effect of residual stress. Investigations of specimens mechanically stress relieved by stretching 2 or 2½% failed to show any effect of stress relieving.[1,4] Tensile tests of large weldments, both as-welded and low-temperature stress-relieved, also failed to demonstrate the improvement in the stress-relieved specimens.[5] It is believed, however, that these and many other tests did not fairly evaluate the residual stress effect, as a strain of 2% may conceivably harm the metal, and the tensile tests were carried out in the ductile range where the stresses at fracture were well above the yield point.

However, in a series of bend tests of large welded plates, Greene showed that use of the low-temperature stress-relieving process considerably increased resistance to fracture.[6] This work was the first to demonstrate by

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the use of test specimens that beneficial results may be obtained by the relief of stress.

During the discussion of this paper, it was suggested that the slight amount of straining and/or the moderate temperatures which accompany this process may have brought about critical improvements, especially in the plastic properties of the metal. In the application of this process, the weld metal and adjacent base metal are strained a small amount in the order of 0.15%, with the probable limits at 0.1 and 0.2%. Also, the areas under the torches are heated to 350–400° F. (176–204° C.), and the weld is warmed to about 150° F. (65° C.). As the effect of these seemingly minor factors had not been considered in Greene's work, it seemed justified at this time to make a supplementary study of the effect on both plate and welds of small amounts of plastic strain and of heating to these temperatures to be sure that these conditions had not influenced Greene's results.

PART I—METALLURGICAL EFFECTS OF LOW-TEMPERATURE STRESS RELIEVING

This experimental work on the metallurgical effects of the low-temperature stress-relieving process was carried out by means of a series of notched slow-bend specimens, and the data were evaluated in terms of the effect of the treatments on transition temperature. In addition, a comprehensive series of standard mechanical tests was made on specimens taken from as-welded and low-temperature stress-relieved weldments.

Table 1—Composition of A-201 Steel, %

C	0.20
Mn	0.58
Si	0.29
S	0.026
P	0.024
Cr	0.036
Cu	0.13
Ni	0.068
Sol. Al	0.0025
Mo	0.001
O	0.0054
N	0.0032
H	0.000195

Table 2—Welding Conditions, Machine-welded Bead Welds

Electrode	E6010
Diameter, in.	5/16
Current supply	D. C. reverse
O.C.V.	75
Current, amp.	175
Voltage, v.	26
Travel, ipm.	6

The first series of tests was made on bead welds in 3/4-in. plate of fully-killed A.S.T.M. A-201, Grade B, firebox quality. Its composition is given in Table 1. The bead welds were machine welded under the conditions given in Table 2.

The specimen used throughout this investigation is shown with the testing jig in Fig. 1A. It consisted of a plate 8 by 2.875 in. which, in welded specimens,

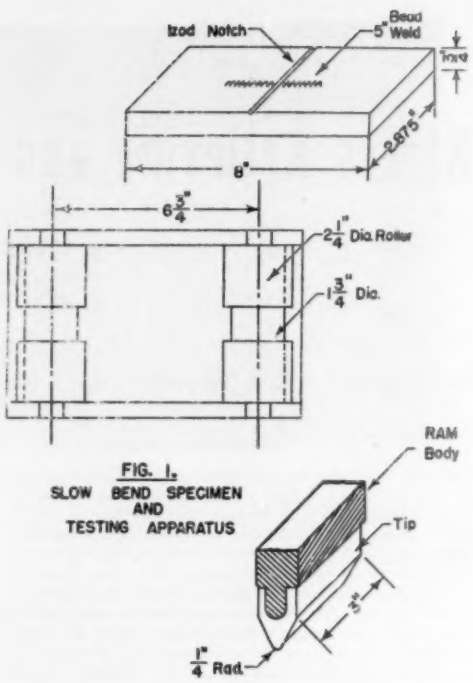

FIG. 1. SLOW BEND SPECIMEN AND TESTING APPARATUS

contained a 5-in. beadweld on one side, the weld being parallel to the direction of rolling. The tolerance on the machined edges of the specimens was ±0.005 in. The weld and plate were notched transversely with an Izod notch to a depth of 0.05 in. The testing was done on a 6 3/4-in. span.

The specimens for testing at other than room temperature were slowly heated or cooled to the desired temperature, held for about 10 min., and quickly transferred to the jig mounted on the testing machine. The removable tip of the plunger was heated or cooled with the specimens. All specimens were held at room temperature for 7 to 10 days between welding and testing.

The following test data were recorded or measured for each of the specimens tested:

1. Angle of bend at maximum load.
2. Lateral contraction 1/32 in. below the notch.
3. Autographic load-deflection diagram.

Transition Temperature

Previous experience in these laboratories has favored the use of 1% lateral contraction for the definition of transition temperature. However, lateral contraction could not be used throughout this investigation because some of the measurements made on early specimens were in error due to a neglect of the contraction which had occurred during prestraining. The angle of bend at maximum load was, therefore, used for plotting all

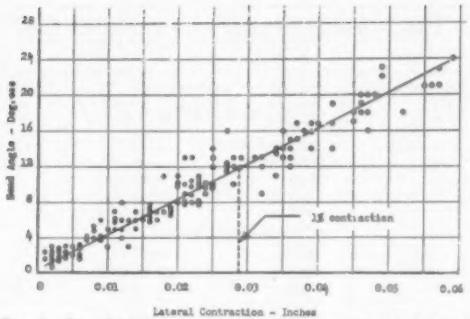

Fig. 2 Correlation between lateral contraction and bend angle

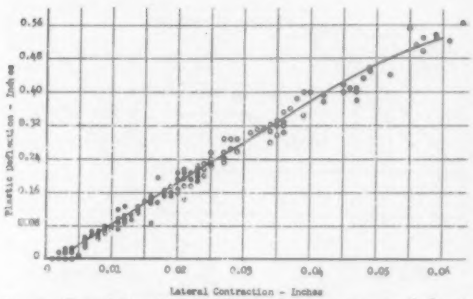

Fig. 4 Correlation between lateral contraction and plastic deflection

transition-temperature curves. In order to be consistent with previous work, a correlation was obtained between lateral contraction and bend angle for a very large number of specimens. This is shown in Fig. 2 and from this it is clear that 1% lateral contraction corresponds to a 12° bend angle. Accordingly, the transition temperature was considered to be the temperature at which the bend angle equaled 12°. The curves were plotted by drawing straight lines between the average points, and in case a change of slope occurred near the transition temperature, an average temperature was used.

In connection with the choice of criterion for transition temperature and on the basis that low plastic flow is the essential requirement, the load-deflection diagram was examined with particular care, as it is automatically drawn during the test and is a good representation of what takes place during the test. Any measurement taken from this diagram can be rechecked at some later time, which unfortunately is not the case with bend angle. It was considered that a good ductility criterion would be the amount of deformation taking place after the elastic limit had been exceeded, since a truly brittle failure is one which occurs with no ductility and thus no plastic deformation. This type of measurement has been used in earlier tests[7] and is easily made, as illustrated in Fig. 3. The correlation with lateral contraction was considered quite good (Fig. 4). The measurement is perhaps even easier than lateral contraction because of the distortion around the notch after fracture. Although it is not proposed at this time to use this relationship, the method of determining the initial plastic flow seems to offer considerable promise.

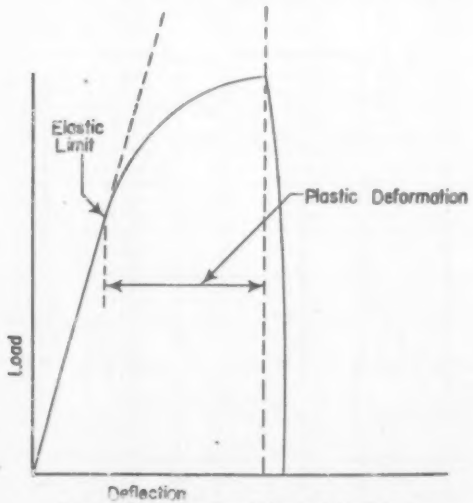

Fig. 3 Measurement of plastic deformation

Physical Tests

In order to provide a thorough metallurgical investigation, extensive series of physical tests were made on as-welded and low-temperature stress-relieved submerged-melt weldments. The low-temperature stress-relieving equipment and method have been described in detail in previous publications.[6, 8]

The results of tensile tests appear in Table 3. Longitudinal and transverse 0.505-in. specimens were taken from the as-received plate, and longitudinal specimens were taken from the area which had been heated to 350–400° F. in low-temperature stress-relieved plate.

Table 3—Results of Tensile Tests

Specimen	*Yield strength, psi.*	*Tensile strength, psi.*	*Elongation in 2 in., %*	*Reduction of area, %*
Plate as-received				
Longitudinal	36,700	67,300	35.0	61.2
Longitudinal	38,650	66,900	35.0	61.1
Transverse	36,500	66,750	35.0	60.7
Transverse	36,100	66,900	34.5	60.6
Zone heated by stress-relieving torches				
Longitudinal	33,500	64,000	38.0	66.5
Longitudinal	33,400	63,700	37.0	63.4
Transverse weld coupons*				
As-welded	42,000	71,200	28.0	...
As-welded	42,000	71,200	28.0	...
L.T. S.R.	42,600	71,400	28.0	...
L.T. S.R.	41,900	71,000	27.0	...

* Weld reinforcement ground off; all specimens failed in base plate.

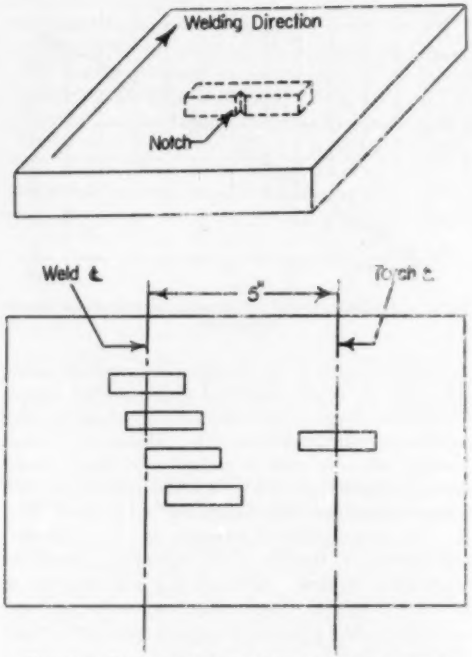

Figure 5 Location and notching direction of impact specimens

It may be noted that very little difference exists in the normal tensile properties of the prime plate in the longitudinal and transverse directions. The specimens from the zone heated during the stress-relieving process had a slightly lower tensile strength and somewhat more ductility than the as-received specimens. Transverse flat tensile specimens taken across the weld zone from as-welded and low-temperature stress-relieved weldments disclosed no difference between the two welded plates.

An extensive series of impact specimens was also tested from both as-welded and low-temperature stress-relieved weldments. Charpy vee-notched specimens were used, and the specimens were removed and notched in the manner shown in Fig. 5. They were notched perpendicular to the plate surface to minimize the effect of any laminations in the plate. This direction of notching made it relatively impossible to distinguish between the properties of specific areas in the weld and heat-affected zones, because the notch passed through more than one area, due to the contour of the weld. It was felt, however, that a good over-all knowledge of the impact properties would be obtained by taking one series each from the weld and the heat-affected zone, and by taking others from the base metal which had been affected in the low-temperature stress-relieving process. Therefore, specimens were notched at the weld center line, in the heat-affected zone, and 1 in., $1^1/_2$ in. and 5 in. from the weld center line. The last-named specimen was notched at the center of the torch-heated area in the low-temperature stress-relieved weldment.

Table 4—Results of Charpy Vee-Notch Impact Tests

As-welded		*Low-temperature Stress-relieved*	
Temperature, ° C.	*Ft.-lb.*	*Temperature, ° C.*	*Ft.-lb.*
Center line of weld metal			
+40	46.0	+40	57.0
+20	26.2, 25.0	+20	27.8, 17.8
0	21.2, 18.2	0	20.0, 20.8
−20	14.0	−20	17.7
−40	12.8	−40	12.0
−60	6.0	−60	6.9
Heat-affected zone			
+40	78.0	+40	79.5
+20	82.0	+20	76.9
0	51.0, 40.8	0	44.8, 38.8
−20	21.2, 5.5	−20	28.0, 27.4
−40	15.4	−40	14.4
−60	5.0	−60	5.8
1 In. from weld center line			
+40	39.0, 33.8	+40	56.0, 61.0
+20	31.0, 23.2	+20	41.8, 33.0
0	6.0, 7.0	0	8.0, 7.8
−20	6.0, 4.8	−20	5.0, 4.8
$1^1/_2$ In. from weld center line			
+40	47.5	+40	52.5
+20	17.8, 26.0	+20	29.0, 32.0
0	12.1, 15.0	0	7.9, 10.2
−20	4.7, 5.0	−20	10.2, 5.0
−40	4.2	−40	4.2
5 In. from weld center line			
+40	54.0, 52.0	+40	63.5, 45.0
+20	33.0, 30.0	+20	37.0, 51.0
0	10.0, 8.6	0	20.2, 15.8
−20	4.0, 7.0	−20	6.0, 5.8

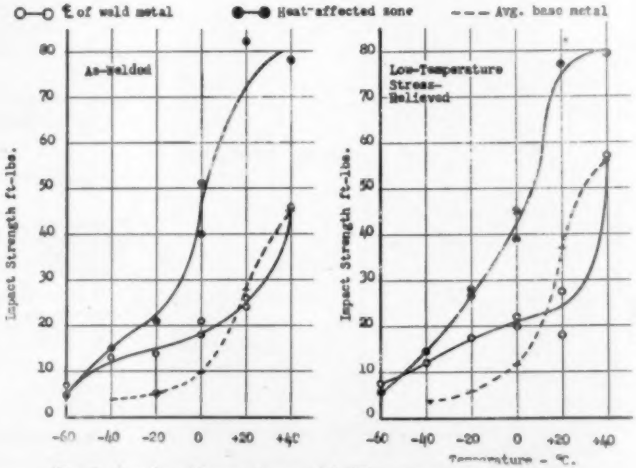

Fig. 6 Results of impact tests with Charpy vee-notch specimens

Table 5—Results of Slow-Bend Tests—A-201 Steel

Testing temperature, ° C.	Maximum load, lb.	Bend angle maximum load, °	Lateral contraction, in.	Plastic deformation, in.	Type fracture
		As-welded			
+22	18,100	24	0.067	0.584	B_1
	19,240	26	0.078	0.684	C_2
	16,740	18	0.047	0.408	C_2
0	17,220	17	0.046	0.392	C_2
	19,080	27	0.070	0.632	C_2
	16,480	13	0.033	0.312	C_2
	17,400	17	0.039	0.404	C_2
−10	17,160	13	0.040	0.344	C_2
−20	16,360	12	0.033	0.272	C_2
	15,600	10	0.021	0.200	C_2
	16,960	14	0.034	0.308	C_2
−40	15,840	10	0.028	0.220	C_2
	15,480	6	0.017	0.196	C_2
−60	13,720	3	0.015	0.056	C_2
	13,460	2	0.005	0.008	C_2
	13,760	3	0.006	0.040	C_2
−78	14,080	2	0.003	0.028	C_2
	13,580	2	0.003	0	C_1
		Plate, as-received			
+22	18,790	34	0.085	0.792	C_2
0	19,160	32	0.071	0.712	C_2
−40	18,440	23	0.047	0.480	C_2
−78	18,760	18	0.041	0.400	C_2
−100	18,400	3	0.001	0	C_1
		Welds—furnace stress-relieved			
+22	19,460	43	0.129	1.064	B_2
	19,400	45	0.117	1.028	C_2
0	19,800	51	0.120	1.132	C_2
	19,640	46	0.110	1.076	C_2
−20	19,940	32	0.080	0.760	C_2
−40	18,520	21	0.055	0.552	C_2
	19,860	34	0.078	0.744	C_2
	20,000	33	0.080	0.752	C_2
−60	17,880	14	0.042	0.392	C_2
	19,780	32	0.072	0.692	C_2
−78	17,320	10	0.028	0.264	C_2
	18,040	14	0.036	0.352	C_2
	15,700	11	0.033	0.240	C_2
−100	16,480	2	0.002	0	C_2
	17,260	4	0.005	0.016	C_2
	17,200	5	0.005	0.024	C_2
		Welds—preheated to 320° C.			
−20	20,500	34	0.083	0.820	B_2
−40	18,920	19	0.046	0.448	C_2
−60	19,440	20	0.046	0.440	C_2
−85	15,740	4	0.008	0.048	C_2
		Plate—Heated 1 hr. at +150° C.			
0	20,080	49	0.124	1.044	B_2
−40	20,150	38	0.086	0.816	C_2
−78	20,300	28	0.072	0.640	C_2
−100	17,200	3	0.002	0.016	C_2
		Plate—strained 0.2% at −30° C.			
0	20,900	45	0.116	1.080	B_2
−40	21,660	40	0.091	0.876	C_2
−78	21,140	28	0.071	0.676	C_2
−85	19,260	17	0.040	0.404	C_2
−90	15,400	4	0.007	0.056	C_2
−100	13,720	2	0.001	0	C_1
		Plate—strained 0.2% at +150° C.			
0	21,480	44	0.107	1.000	C_2
−40	21,260	33	0.076	0.764	C_2
−78	21,640	32	0.073	0.716	C_2
−85	16,560	7	0.016	0.140	C_2
−90	15,720	6	0.009	0.064	C_2
−100	17,200	6	0.016	0.136	C_2
		Welds cooled 10 min. at −30° C.			
0	18,760	21	0.057	0.496	C_2
	19,000	23	0.063	0.564	C_2
−20	17,800	19	0.042	0.376	C_2
	15,440	10	0.022	0.192	C_2
−40	15,100	7	0.017	0.136	C_2
	15,900	8	0.022	0.180	C_2
−60	16,400	10	0.020	0.168	C_2
	14,800	6	0.012	0.088	C_1
		Welds heated 10 min. at +75° C.			
0	18,240	24	0.059	0.532	C_2
	18,360	26	0.059	0.528	C_2
−20	15,260	10	0.023	0.224	C_2
	17,080	14	0.035	0.328	C_2
−40	15,760	10	0.024	0.224	C_2
	16,540	10	0.024	0.216	C_2
−60	14,520	6	0.013	0.092	C_2
	15,060	7	0.014	0.104	C_2
		Welds heated 10 min. at +320° C.			
0	15,820	13	0.029	0.288	C_2
	18,440	23	0.057	0.528	C_2
−20	16,840	12	0.032	0.312	C_2
	15,840	12	0.025	0.236	C_2
−40	17,300	14	0.034	0.324	C_2
	17,000	12	0.027	0.260	C_2
−60	15,800	7	0.019	0.168	C_2
	13,660	5	0.008	0.056	C_2
		Welds strained 0.1% at −30° C.			
+22	18,620	24	...	0.548	C_2
0	18,500	25	...	0.484	C_2
−40	16,000	11	...	0.188	C_2
−60	17,700	13	...	0.296	C_2
−78	16,480	9	...	0.184	C_2
		Welds strained 0.1% at +150° C.			
+22	18,620	26	...	0.580	C_2
0	17,540	22	...	0.384	C_2
−40	17,300	12	...	0.272	C_2
−60	16,380	9	...	0.188	C_2
−78	14,400	6	...	0.056	C_2
		Welds strained 0.2% at −30° C.			
0	17,120	16	0.038	0.364	C_2
	16,860	17	0.037	0.332	C_2
−20	16,260	16	0.027	0.244	C_2
	16,360	12	0.028	0.264	C_2
−40	15,460	11	0.018	0.152	C_2
	16,360	10	0.025	0.240	C_2
−60	16,740	9	0.021	0.192	C_2
	17,240	13	0.025	0.232	C_2
−78	16,500	8	0.021	0.144	C_2
	15,320	6	0.011	0.072	C_2
		Welds strained 0.2% at +75° C.			
+22	16,960	23	0.049	0.448	C_2
	18,300	28	0.077	0.720	C_2
0	16,160	12	0.029	0.260	C_2
	17,200	17	0.042	0.392	C_2
−20	17,240	12	0.027	0.288	C_2
	17,100	16	0.035	0.332	C_2
−40	16,260	10	0.023	0.212	C_2
	14,300	7	0.016	0.088	C_2
−60	15,600	6	0.016	0.140	C_2
	16,340	8	0.020	0.160	C_2
		Welds strained 0.2% at +320° C.			
+22	17,540	22	0.049	0.452	C_2
	18,300	21	0.056	0.508	C_2
0	17,840	20	0.047	0.400	C_2
	18,040	20	0.048	0.428	C_2
−20	16,700	14	0.025	0.256	C_2
	17,240	13	0.028	0.292	C_2
−40	16,600	13	0.022	0.236	C_2
	17,560	12	0.027	0.232	C_2
−60	16,100	6	0.014	0.128	C_2
	16,760	7	0.019	0.160	C_2
		Welds strained 1.0% at +75° C.			
+22	17,300	18	0.045	0.416	C_2
0	14,760	7	0.016	0.152	C_2
	15,640	10	0.020	0.192	C_2
−20	16,500	13	0.021	0.212	C_2
−40	15,400	8	0.012	0.128	C_2
−60	16,260	7	0.011	0.120	C_2
		Welds strained 1.0% at +320° C.			
+22	18,120	17	0.045	0.400	C_2
0	18,100	15	0.036	0.328	C_2
	17,580	13	0.031	0.304	C_2
−20	18,560	15	0.037	0.356	C_2
−40	17,940	11	0.020	0.212	C_2
−60	15,700	5	0.008	0.064	C_1

The results are given in Table 4 and are shown graphically in Fig. 6, in which the values for the three series of specimens taken from the base metal were averaged and plotted as single broken lines, as these values were similar. In general, differences between the impact properties of the two weldments are so slight that they may be considered negligible. It is interesting to observe that the weld metal and heat-affected zones had the most favorable properties in the low-temperature ranges.

The results of Vickers-hardness surveys of two welded plates are illustrated graphically in Fig. 7. There appeared to be no significant difference between the as-welded and low-temperature stress-relieved weldments.

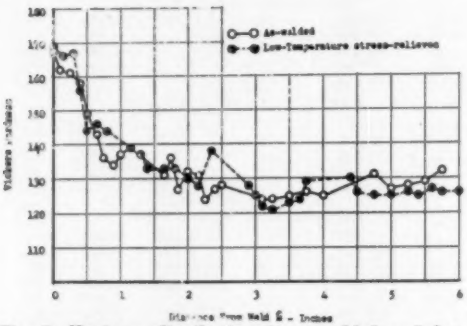

Fig. 7 Hardness distribution in as-welded and low-temperature stress-relieved Unionmelt weldments

Slow-Bend Tests

Several series of bead-welded specimens were tested after being strained small amounts at temperatures ranging from −30 to +320° C., or after being exposed to these temperatures for short times. The treatments at −30° C. were included to determine the effect of straining below the transition temperature. Other treatments were also used to round out the investigation. The data from these tests are compiled in Table 5. The classification of fracture, as given in this table, is the same as that used in a previous publication.[9] The transition temperatures obtained from the curves are shown in Table 6.

Discussion of Tests Results

Standard bead-welded specimens tested in the as-welded condition had a transition temperature of −20° C., while untreated plate specimens had a transition temperature of −86° C. Bead welds which had been furnace stress relieved at 650° C. had a transition temperature of −78° C., and bead welds made on plates preheated to 320° C. had a transition temperature of −66° C. It is apparent from these results that welding considerably raised the transition temperature of the steel plate, and that furnace stress relieving and preheating both tended to lower the transition temperature of the welded specimens. The transition curves are shown in Fig. 8.

Table 6—Transition Temperatures

Specimens	*Transition temperature, ° C.*
As-welded	−20
Prime plate	−86
Welds, furnace stress relieved	−78
Welds, preheated to 320° C.	−66
Plate, strained 0.2% at −30° C.	−87
Plate, strained 0.2% at 150° C.	−84
Plate, heated 1 hr. at 150° C.	−92
Welds, cooled 10 min. at −30° C.	−26
Welds, heated 10 min. at 75° C.	−20
Welds, heated 10 min. at 320° C.	−31
Welds, strained 0.1% at −30° C.	−51
Welds, strained 0.1% at 150° C.	−40
Welds, strained 0.2% at −30° C.	−30
Welds, strained 0.2% at 75° C.	−26
Welds, strained 0.2% at 320° C.	−41
Welds, strained 1.0% at 75° C.	−8
Welds, strained 1.0% at 320° C.	−34

Effect of Straining on Plate

To determine whether straining would affect the base metal, unwelded plate specimens were strained 0.2% permanent set at −30 and 150° C., and were also heated for 1 hr. at 150° C. before testing. The transition curves of these specimens shown in Fig. 9 indicate that the plate material was not affected by these tests.

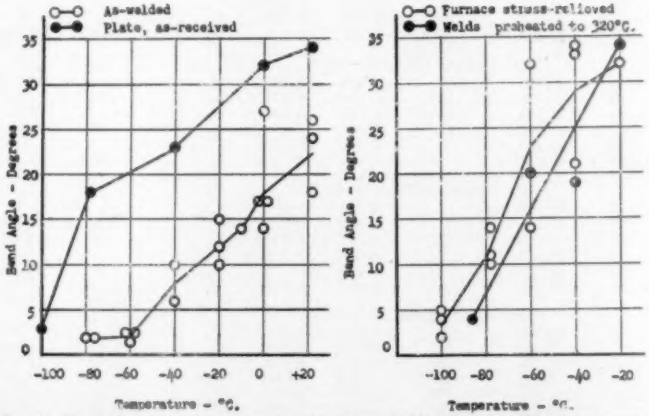

Fig. 8 Transition-temperature curves for as-welded and prime plate specimens, and the effect of furnace stress relieving and preheating on bead welds

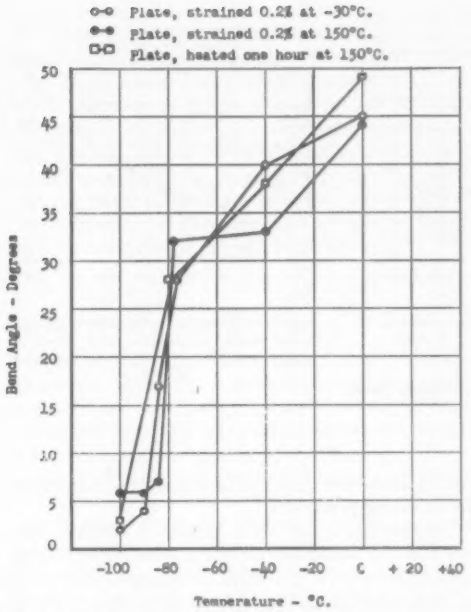

Fig. 9 Transition-temperature curves showing the effect of various treatments on unwelded plate

Effect of Temperature on Bead Welds

Bead-welded specimens were heated or cooled for 10 min. at −30, 75 and 320° C. The respective transition temperatures were −26, −20 and −31° C., as shown by the curves in Fig. 10. The lowering of the transition temperature 6° by cooling to −30° C. was not considered significant. The 11° improvement gained by heating to 320° C. may or may not be significant, as the curve levels off at the critical 12° bend angle, and the transition temperature reported is an average value. The treatment at 75° C. which is the temperature to which the weld is heated in the low-temperature stress-relieving process, showed no change from the as-welded condition.

Effect of Straining on Bead Welds

Bead-welded specimens were strained 0.1% permanent set at −30 and 150° C.; 0.2% at −30, 75 and 320° C.; and 1.0% at 75 and 320° C. The transition curves for these tests are shown in Figs. 10 and 11. All straining was done prior to notching, so that the strain would be uniform.

Both series of specimens strained 0.1% were apparently improved over the as-welded specimens, as the transition temperatures were −51 and −40° C., for the two series of specimens. The series strained at −30° C. had the lower transition temperature.

The specimens strained 0.2% had transition temperatures of −26, −30 and −41° C. As may be seen in Fig. 11, the points are well mixed, and the difference between the curves was slight.

The specimens strained 1.0% had transition temperatures of −8 and −34° C., with the series strained at 75° C. having the higher and the series at 320° C. having the lower. The improvement at 320° C. is probably, at least partially, due to the effect of heating to that temperature.

Neglecting the effect of temperature, the data for various degrees of strain are consolidated in Fig. 12. These curves illustrate the slight trend established by these tests, namely, that the transition temperature is apparently increased slightly as the amount of strain is increased and that very small strains may be beneficial to the bead-welded specimens. Straining by as much as 1.0% may be harmful.

Summary

It is apparent from the results of this part of the investigation that very little, if any, difference exists between the mechanical properties of as-welded and low-temperature stress-relieved weldments. It was indicated, through change in transition temperature of slow-bend specimens, that small amounts of plastic strain may be beneficial with respect to the ductility of the weld zone. However, when these data are examined with respect to the amount of straining and the temperature at which this straining occurs in the low-temperature stress-relieving process, it may be seen that the improvement is relatively slight. It should, of course, be pointed out that these conclusions were obtained by testing uniformly stressed members and

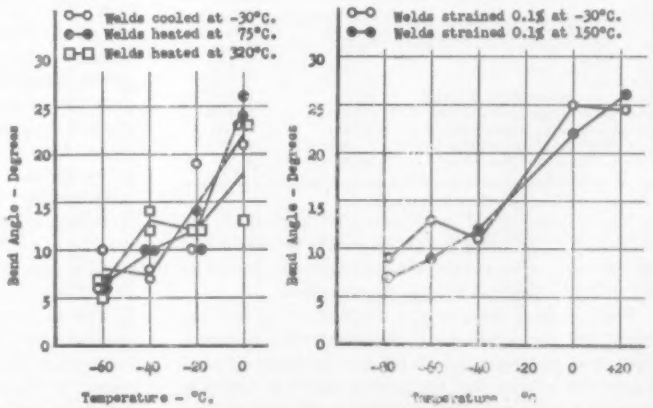
Welds cooled at -30°C.
Welds heated at 75°C.
Welds heated at 320°C.
Welds strained 0.1% at -30°C.
Welds strained 0.1% at 150°C.
Bend Angle - Degrees
Temperature - °C.

Fig. 10 Transition-temperature curves showing the effect of heating, cooling and straining on bead welds

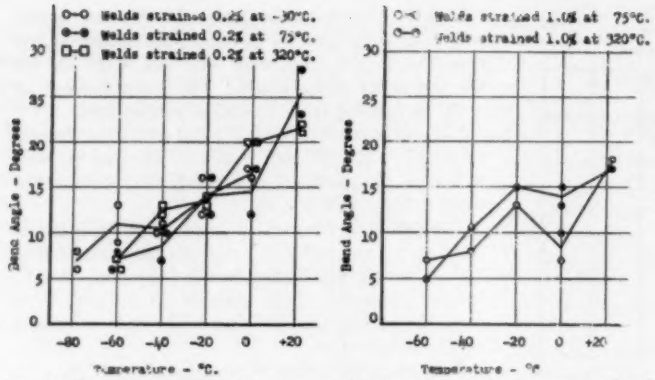
Welds strained 0.2% at -30°C.
Welds strained 0.2% at 75°C.
Welds strained 0.2% at 320°C.
Welds strained 1.0% at 75°C.
Welds strained 1.0% at 320°C.
Bend Angle - Degrees
Temperature - °C.

Fig. 11 Transition-temperature curves showing the effect of straining bead welds 0.2 and 1.0% at various temperatures

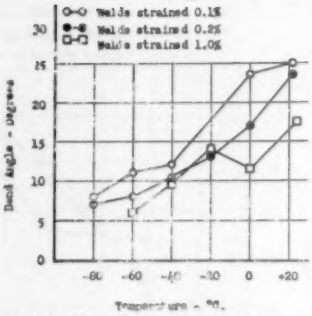

Fig. 12 The effect of small strains on bead welds, neglecting the effect of temperature at which straining took place

may not necessarily apply to vessels that have been hydraulically stress-relieved in which the presence of stress concentration may cause large local plastic deformation with consequent harmful results.

On the basis of these results, it was concluded that so-called metallurgical changes in the low-temperature stress-relieving process could not rationally account for the large improvements illustrated by Greene's bend tests.

PART II—ANALYSIS OF RESIDUAL STRESS

As the general conclusion from Part I was that no appreciable metallurgical effect is obtained from the heating and straining accompanying the low-temperature stress-relieving process, it was clear that further study of the technique and testing conditions used in Greene's test was necessary. In studying his work, the following significant data were taken from specimens tested at 0° F. (−18° C.):

Treatment	Conditions at initial crack: Stress, psi.	Deflection, in.
As-welded	8,750	0.044
As-welded	6,000	0.062
As-welded	11,900	0.078
Low-temperature stress relieved	56,700	0.330
Low-temperature stress relieved	59,000	0.360
Low-temperature stress relieved	69,800	0.850
Low-temperature stress relieved	67,800	0.575
Low-temperature stress relieved	56,200	0.307

The importance of conditions that prevail at the instant of initial crack formation cannot be overlooked as the cracks thus formed actually represent failure so far as brittle fracture studies are concerned.

From study of these data, it will be noted in the first place, that the deflections of the as-welded plates were very small as compared with those of the stress-relieved plates, and second, that the loads at failure in Greene's tests were very low in the as-welded specimens but were high for the low-temperature stress-relieved plates. The significance of these two factors will be clearer from the following discussion.

The first significant fact in respect to Greene's data was that the deflections at initial cracking of the as-welded plates were very small as compared with those of stress-relieved plates. These tests were made of 30-in. long welds on a span of 23 in., and computation shows that failure occurred at bend angles of less than 1° with the as-welded plates. When it is realized that in previous work recorded in the literature, transition temperature has been judged at bend angles ranging from 12 to 60°, it is clear that these specimens failed under conditions of very low plasticity. The 12° bend angle corresponds to about 1% lateral contraction, a value that has been considered to represent a relatively high degree of brittleness. The essentially brittle state attained under Greene's conditions is further borne out by the performance of one as-welded specimen which was not loaded externally at all but failed because of the slight additional stress associated with cooling and subsequent warming by about 5° F. to testing temperature. This, of course, is not too unusual, as this type of failure has occurred all too often in practice. Under Greene's conditions, therefore, plastic flow had been almost completely suppressed, and it seems logical to draw the conclusion that one of the principal elements of a test designed to disclose the effect of residual stress is the essential absence of general or local plastic flow before failure.

Apparently, from these observations, the criterion of failure in judging the influence of residual stress must be in a different order of magnitude than has been used for appraising the effects of normal variables, such as chemical composition or heat effects in welded steel. This is so because of the different mode of effect of residual stress as compared with metallurgical variables. The need for determining the effect of residual stress by tests in which no plastic flow can occur is parallel to the requirement in any test that the test must reflect the factor being investigated and must not modify the

factor in the course of the test. For instance, if it is desired to determine the effect of pearlite grain size on steel, no tests can be made above the upper critical point because above that temperature pearlite does not exist. Likewise, as it has been established that residual stress is eliminated or nullified in proportion to plastic strain, then any test that involves such straining of the metal is invalid as a measure of the effect of residual stress. Therefore, to measure the effect of residual stress, the test must not involve plastic flow but must be conducted in the brittle condition.

Suppression of Plastic Flow

It has been established, tentatively, that plastic flow must not occur during a test serving to evaluate residual stress. There now remains the important step of determining whether conditions exist under which specimens will fracture without plastic deformation.

The method of measuring the amount of plastic flow is quite important, as it is essential to determine small amounts and to explore small areas because, as the fractures are essentially brittle, the deformed metal is located only in a narrow band adjacent to the fracture. Hardness measurement would seem to be particularly applicable as good correlation can be expected between the amount of plastic flow and the increase in hardness, and relatively small areas can be surveyed with standard equipment.

Therefore, hardness surveys were made on the fractured edges of unwelded slow-bend specimens in both the as-received and 0.2% strained conditions. The composition of the steel is given in Table 1 and the results of the tests are shown in Fig. 13. Strain-hardness equivalents were derived by making hardness measurements on small specimens uniformly strained in the tensile machine.

As can be seen from Fig. 13, at a temperature of −100° C. macroscopic strain as indicated by changes in hardness is completely suppressed in the particular test specimen chosen. Such a specimen clearly broke in a completely brittle manner. Also differences in ductility between strained and unstrained metal are apparent only at large strains and relatively elevated temperature. The behavior at low strains was identical for each specimen. Additional tests were run on the fracture faces of Charpy specimens from the same material, and these results are shown in Fig. 14. The same general behavior is again noted, although in the case of the Charpy specimen, loss of capacity to deform occurs at much higher temperatures, illustrating the more severe test conditions.

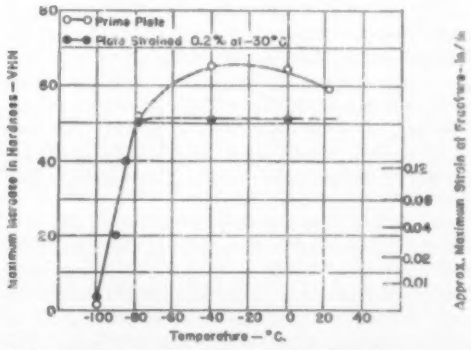
Prime Plate
Plate Strained 0.2% at −30°C.
Maximum Increase in Hardness — VHN
Approx. Maximum Strain at Fracture — in/in
Temperature — °C.

Fig. 13 Strain hardening in slow-bend specimens as a function of testing temperature

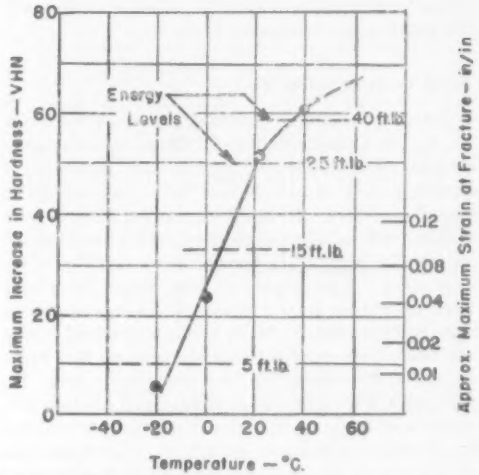

Fig. 14 Strain hardening in Charpy vee-notch specimens as a function of testing temperature

Other investigators have previously shown that this same suppression of ductility may be achieved without the presence of notches, merely by testing a tensile specimen at sufficiently low temperatures. Significant data from the work of Moore and Green[10] are shown in Fig. 15. Since this work indicated that it was possible to completely suppress macroscopic ductility even in unnotched uniaxially-stressed specimens, it was decided to conduct experiments in which wide plates with and

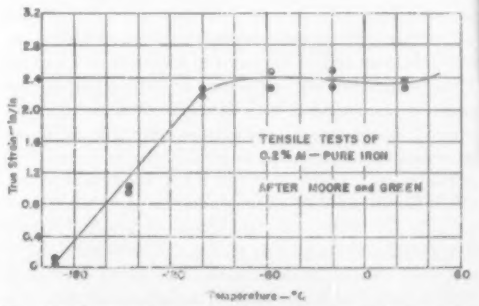

Fig. 15 Suppression of straining in unnotched tensile specimens by lowering testing temperature

without residual stresses would be tested at a sufficiently low temperature to suppress strain.

Bend Tests at −150° C.

For this work, four unwelded plates, 18 by 20 by ¾ in., were used. In two of these plates residual stresses were induced by rapidly and progressively heating to 500° F. a 1-in. wide band along the center line of the plate. In this way residual stresses were induced without appreciable effect on the metallurgical structure of plate. The remaining two plates were left as-received. The specimens were tested by simple beam loading on a 15-in. span. The temperature was lowered to −150° C., where it was maintained during the test. This was the lowest temperature that could be maintained under the test conditions. After the specimens were fractured, careful hardness measurements were made to determine the increase in hardness at the region of fracture initiation. This region was determined by examining the typical chevron patterns of the fracture. The results of these tests are shown in Table 7.

Table 7—Bend Tests of 18- by 20- by ¾-In. Unwelded Plates, Tested at −150° C.

Treatment	*Load, lb.*	*Deflection, in.*	*Hardness increase, V.H.N.*
Prime plate	75,800	...	0
Prime plate	70,600*	1 1/16	...
Differentially heated	55,900	...	7
Differentially heated	79,400	...	14

* No failure.

It is apparent that the severity of the conditions under which these tests were made was not sufficient to completely suppress plastic flow in all cases. One prime plate specimen did not fracture at all and exhibited considerable ductility before the test was stopped. However, the other prime plate specimen fractured in a typically sudden, brittle manner, and no trace of hardening could be found in the area of fracture initiation, indicating that no appreciable plastic deformation had occurred. As the conditions were the same in both cases, these results seem to indicate that the tests were carried out in a transition zone, such as is found with Charpy specimens, where the scatter is very large.

Both the differentially-heated specimens fractured in a brittle manner but increases in hardness of 7 and 14 Vickers numbers were observed, indicating that appreciable local straining had occurred even though the over-all strain was exceedingly small. The hardness increases were equivalent to local strains of 1 and 2%, respectively. The local strain which took place in the two differentially-heated bend specimens was sufficient to partially wipe out the effect of residual stress in one case and completely nullify it in the other. It is proposed that if the conditions of no plastic flow had been met, the differences would have been significantly greater.

The second significant factor in Greene's data concerned the load-carrying capacity of the plates. The significance of the differences noted by him will be explained by the following discussion.

Discussion of Residual Stress in Weldments

As is well known, a weldment normally contains residual longitudinal tensile stress in the weld zone of approximately yield-point value. If an external tensile load is applied to this weldment, the stresses will build up to the yield point in the weld zone, while the elastic stress in the rest of the weldment is low. Thus, the effect of residual stress is that of increasing the unit stress in the weld zone to yield-point value at very low external loads, with the result that the weld metal may undergo plastic deformation while the base metal is being only elastically strained.

In any assembly, failure occurs when the total induced strain equals or exceeds the capabilities of the metal to undergo that amount of deformation, either plastic or elastic. Obviously, if the metal in the zone of maximum stress is capable of straining, plastic flow takes place, thus nullifying residual stress and ductile failure or no failure occurs. Such a condition exists in the normal tensile or bend testing of welded plates, even of unusually large size, and in adequately designed structures made from good-quality material and with high-quality welding. For this reason, the effects of residual stress have not been demonstrable with most of the test specimens employed and have not been influential in affecting the service life of many adequately designed and constructed assemblies.

However, if conditions are such that plastic flow is almost entirely inhibited in the zone of maximum stress, then this zone is very likely to fail in a brittle manner. That such a condition is frequently created is evidenced by a long list of brittle failures, particularly with respect to welded ships. Many factors such as low temperature, the presence of notches or cracks, increased strain rate, and general design characteristics, which aggravate the multiaxial stress conditions, may combine to inhibit plastic deformation. Furthermore, in a given structure, the embrittling conditions do not necessarily remain constant. Temperature will obviously change if the structure is exposed to the elements, and the presence and/or sharpness of notches and cracks may also change, particularly with respect to fatigue cracks, which may develop during service. In such structures, therefore, where no opportunity is provided for their relief, residual stresses may have a very important bearing on service performance. This will be clearer from the following line of thought.

Assume a welded assembly or plate with the weld zone having a normal residual stress in the order of the yield point, about 45,000 psi. As this plate is progressively loaded externally, an increase in unit load in the order of 10,000 psi. will automatically bring about or exceed yield strength loading in the weld zone. The natural requirement then is for plastic straining to occur

in the weld zone in spite of the fact that the nominal stress in the plate is low. Under most conditions, such straining occurs, and the load on the weld zone does not increase until the average load on the specimen equalizes. Residual stress is, therefore, eliminated as an effective variable. However, under the conditions described previously, where plastic flow cannot occur because of low temperature, the presence of a sharp notch, and/or high-strain rate, residual stress becomes very important. Although it still creates only a yield-point condition in the weld zone, this is equivalent, under the imposed conditions, to the fracture stress. Failure by fracture with no general deformation can thus occur at what seems to be a low nominal loading of the structure.

If, at this point, we again observe Greene's data and study it in the light of the above analysis, the following conditions will be found to prevail:

Treatment	*Computed stress at initial crack, psi.*		*Nominal residual stress, psi.*		*Effective stress at initial crack, psi*
As-welded	8,750	+	45,000	=	53,750
As-welded	6,000	+	45,000	=	51,000
As-welded	11,900	+	45,000	=	56,900

Of course, these figures are not absolute values as the residual stress was estimated from previous work, but the total effective stress on the weld zone in these welds is in the right order of magnitude and corresponds closely to the stress at which the low-temperature stress-relieved welds failed. These welds were in the so-called restrained condition, having an effective notch and being tested at relatively low temperature, and consequently were in a state in which plastic flow was restricted. The cracking of the welded plates under very low external loading supports the soundness of the reasoning that the presence of residual stress had been very important under these test conditions.

It should be pointed out that the literature includes several references to similar test conditions or observed service conditions that led to the same type of failure. Of particular pertinence is the work of Vanderbeck and Gensamer,[11] who pointed out that in welded ship failures the calculated nominal stress in the crown of the deck of a ship at time of fracture varied from 3315 to 12,200 psi. The values check closely with those obtained in Greene's tests of as-welded plates.

In the light of this general discussion, it is proposed that residual stress may be very important in the failure of welded structures under conditions that inhibit plastic flow. This effect is illustrated graphically in Fig. 16, which shows that with a combination of residual stress and no plastic flow, failure would occur at relatively low loads. However, as residual stress is progressively relieved by increasing amounts of plastic flow prior to failure, the load-carrying capacity would increase. The results of the bend tests at −150° C. are plotted on this curve. The position of these points

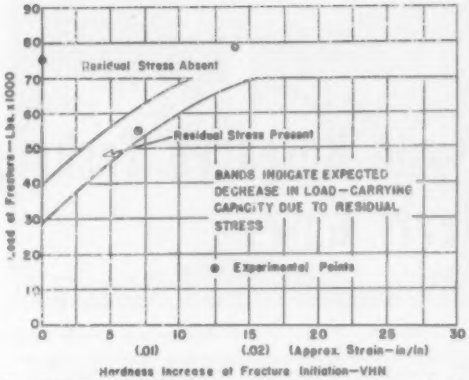

Fig. 16 Postulated effect of residual stress on load-carrying capacity as a function of strain hardening

shows why the full effect of residual stress was not demonstrated in these tests.

CONCLUSIONS

Exploratory investigations have been made of the metallurgical effects of the low-temperature stress-relieving process and of the factors that are necessary in order to investigate the effect of residual stress. The results appear to justify the following conclusions:

1. There is no appreciable metallurgical effect, harmful or otherwise, from the heating and straining accompanying the low-temperature stress-relieving process.

2. In order to investigate the effect of residual stress, tests must be made under conditions that inhibit plastic flow even of a local nature on the fracture surface.

3. Under conditions whereby plastic deformation is suppressed, steel structures containing residual tensile stresses may fail at significantly lower loads than stress-free structures.

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Some Research Techniques for Studying Arcs in Inert Gases

Selected techniques for obtaining fundamental data on inert-gas-shielded welding arcs and interpretations of some of the phenomena observed

by R. H. Gillette and R. T. Breymeier

INTRODUCTION

THIS paper, which is in no sense a complete report, presents some selected techniques for obtaining fundamental data on inert-gas-shielded arcs of the sort used in welding applications. Interpretations are still lacking for a number of the phenomena observed, but it is felt that the techniques themselves, together with some indication of their validity, may be of sufficient interest to warrant presenting them at this time.

The study concerns short, high current, atmospheric pressure arcs in argon or helium with point-to-plane geometry. For the comparatively simple case of the refractory (tungsten) electrode, where no metal transfer in the usual sense occurs, some probe studies are presented with an indication of their applicability to arc calculations. Also a technique for obtaining comparative heat transfer data of considerable utility is described. In the case of arcs with consumable electrodes, ultraslow motion picture investigations of the types of metal transfer characteristic of some different welding conditions and electrode compositions are discussed.

The general approach, of which the present paper is a first progress report, is based on the assumption that the most promising way to formulate an adequate theory of arc behavior is to measure all accessible quantities by as many reliable methods as can be devised and track down any discrepancies. The experimental results are then to be interpreted in terms of known general principles and a series of checks and balances set up so that the validity of each step can be established before proceeding to the next. For example, from a measurement of current density and potential gradient in the plasma the arc temperature can be calculated if the Saha equation (which relates the relative concentrations of charged and neutral particles to the arc temperature and effective ionization potential of the gas) be applicable. Agreement between the value so calculated and the value measured from spectra or velocity of sound—or preferably both—would establish both the validity of the theory used and the reliability of the experimental data. Lack of agreement would necessitate more extensive study. Although it should be possible to build up a satisfactory theory in this way, the available data are not yet sufficient for a conclusive test of the practicality of this approach.

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PROBE MEASUREMENTS

The potential at various points in a low pressure discharge can be measured with considerable accuracy by inserting a fine wire at the point in question and interpreting the characteristic obtained by varying the probe potential and measuring the current flowing to the probe. Langmuir and Mott-Smith[1] put this method on a sound basis and it has since been confirmed by many subsequent studies. As applied to atmospheric pressure arcs, however, the careful study by Mason[2] has shown that great caution must be exercised in interpreting the data. He showed, for example, that introduction of the probe seriously disturbed the arc and made the usual deductions from the shape of the characteristic unreliable. Spraragen and Lengyel,[3] in their well-known review article, conclude that probe measurements are not applicable to atmospheric pressure arcs.

It was felt, nevertheless, that the range of validity of probe measurements should be checked experimentally. The present investigation involves only measuring the arc gradient and, for this purpose, the method which has been most frequently used is measurement

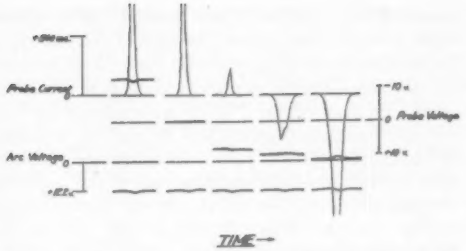

Fig. 1 Oscillograph traces of arc voltage, probe voltage and probe current in a 100 amp. arc in argon for a series of probe voltages as shown

The slight change in probe voltage at the peak of the probe current is due to IR drop in the probe circuit. The change in total arc voltage is seen to be negligible except when the probe is drawing large currents

of total arc voltage as a function of arc length assuming the electrode drops remain constant. A complication in arc measurements, of course, is the fact that the temperature is well above the melting point of any conductor so the probe must be kept in constant motion to prevent its burning up. Mason showed that this disparity between arc and probe temperatures was the chief cause of the disturbances he observed in the long, low current arcs in air which he studied. The arc voltage, for example, increased more than 10% when the probe passed through the arc. In the inert-gas arcs of interest here this point was investigated by sweeping probes of various diameters through the arc and simultaneously recording arc voltage and probe current and voltage on a multichannel oscillograph. A series of traces at different probe voltages for a 5 mm., 100 amp. arc in argon is shown in Fig. 1 where it is seen that the change in arc voltage for this case was negligibly small. Here the arc was drawn between a $^1/_{16}$-in. tungsten electrode and a stainless steel plate (Type 304) and the probe was a 0.020-in. Mo wire swung through the plasma at about 5 cm. per second. A corresponding trace for helium would show a much greater disturbance due to introduction of the probe; a voltage change of the order of 10% being noted. This point is further discussed below.

For measuring the plasma gradient the form of probe finally adopted after testing many designs consists of an endless wire running between massive aluminum pulleys. The setup is shown in Fig. 2 and has the advantage that the probe wire can be located exactly in the desired spot and left there long enough for a careful cathetometer measurement of its position. Its main advantage, however, is the much greater consistency of the results obtained with its use as compared to other types tried.

Using the above arrangement, probe characteristics have been obtained in both argon and helium arcs between a suitable tungsten electrode and a stainless steel plate at currents of 75 and 100 amp. and at arc lengths from 2.5 to 7 mm. More extensive measurements had been made earlier with swinging or oscillating probes. These curves show positive ion saturation at

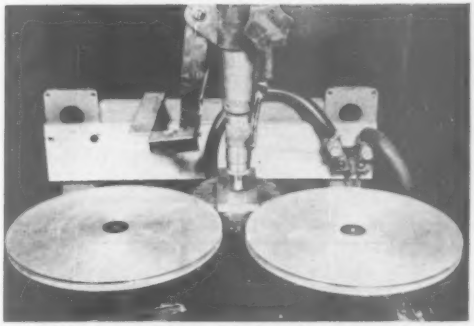

Fig. 2 Equipment used for obtaining probe characteristics

The fine wire which can be rotated at a series of speeds is guided by the small pulley just visible above the large pulley at the right. The location of the probe wire in the arc can be determined with a cathetometer

negative probe potentials and approximately exponential increase in electron current as the potential increases, but there is no indication of electron saturation up to voltages at which the arc transfers to the probe. In the absence of the electron saturation break, the floating potential, or point of zero current, is taken as the plasma potential. However, since only the gradient is currently of interest, this could differ from the true plasma potential by a constant without affecting the result. To determine the plasma gradient it is, of course, necessary to measure the potential at a number of points along the arc axis.

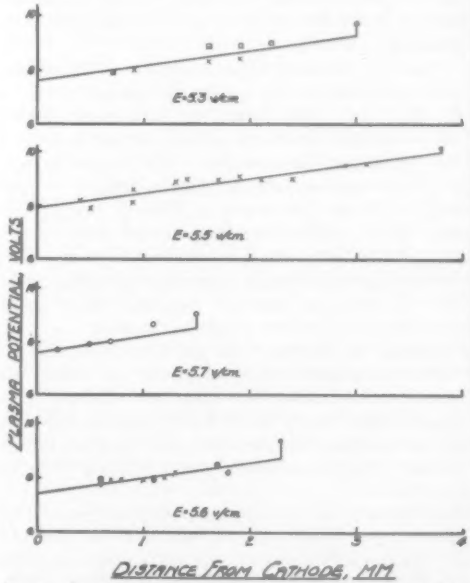

Fig. 3 A series of curves of probe floating potential vs. distance of the probe from the cathode for the arc lengths indicated

The arc in this case was drawn between a $^1/_{16}$-in. tungsten electrode and a stainless steel workpiece and carried 100 amp.

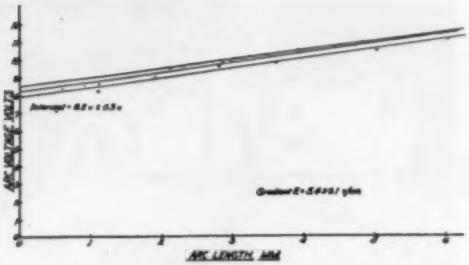

Fig. 4 Three check determinations of the arc voltage vs. arc length curve for the arc as in Fig. 3

The reason for the slight shift is unknown

To check the gradients as obtained above, the arc voltage was also carefully determined as a function of arc length and the slope of the curve measured. Figures 3 and 4 show comparisons of the two methods for a 100 amp. arc in argon between a $^1/_{16}$-in. tungsten electrode and a stainless steel plate. In Fig. 3, the probe measurements at four different arc lengths lead to a voltage gradient of 5.5 ± 0.2 v./cm. Three independent determinations of the arc voltage vs. arc length curve, shown in Fig. 4, lead to 5.4 ± 0.1 v./cm. The agreement between these values, which is generally characteristic of the arc in argon, is seen to be excellent and is actually considerably better than the corresponding values for arcs in helium, where values for check determinations are also far less consistent. As a typical example, for a 75-amp. arc, the arc voltage vs. arc length curve gave 17 ± 2 v./cm. while a single set of probe measurements gave 9 v./cm.

One of the reasons for the discordance of the measurements in helium is the greater disturbance of the arc by the probe; both current and voltage are affected, but in opposite directions so that the power tends to remain substantially constant. This is shown in detail for 3 mm. arcs in Table 1. This constancy of power tends to throw some doubt on Mason's interpretation that the disturbance arose from increase of arc power to compensate for thermal losses to the probe. It is evident that considerably more work using finer probes of more refractory materials is needed before significant data on the helium arc can be obtained.

During the course of the probe measurements evidence was collected which indicates that considerable currents flow outside the luminous envelope of the plasma and a careful search has revealed no indication of the intensely ionized inner core reported for arcs in air. Thus, for example, when a rotating probe was passed through the arc it was found to draw current while it was moving through a distance equal to about twice the diameter of the luminous zone. Furthermore, a probe characteristic can be obtained as much as one centimeter from the arc. These facts indicate the need for more detailed study to find out the exact pattern of current flow to the work. An additional difficulty with current density determinations is the fact that the plasma is definitely bell shaped and not cylindrical as has generally been assumed in the past for the longer arcs heretofore studied.

Using the gradient deduced above, together with the measured current density (based on the diameter of the luminous zone which almost certainly leads to a density somewhat too high as seen above) it is possible to apply the Saha equation to calculate the arc temperature. The necessary relation may be shown to be:

$$\log \frac{j}{X k_e} = -\frac{2520 V_i}{T} + \frac{1}{4} \log T - 0.172 \quad (1)$$

where j = current density (amp./cm.^{2}), X = plasma gradient (v./cm.), k_e = electron mobility (cm./sec./v./cm.), V_i = effective ionization potential (v.), and T = absolute temperature (° K). The values of k_e may be obtained from tables given in Townsend's book[4] and take account of the Ramsauer effect. Calculations from this equation with $V_i = 11.5$ v. (the first excitation potential rather than the actual ionization potential) lead to arc temperatures near 8000° K. which is probably too high, but is certainly of the right order of magnitude. More detailed conclusions must await the results of arc-temperature measurements now being undertaken.

HEAT TRANSFER MEASUREMENTS

From the point of view of welding, the most important characteristic of an arc is its ability to transfer heat to the workpiece, and the fraction of the total energy generated which is so transferred might be thought of as a figure of merit for the operation in question. However, this information is in itself not sufficient because a portion of the energy which is transmitted at thermal levels below the melting point of the work cannot be fully effective in the welding process even though it may assist in heating the piece and consequently may make the melting more extensive than it would otherwise be. It was felt that a procedure was needed which was capable of measuring total heat transfer and also of giving some indication of the thermal level at which it was transferred. The procedure finally adopted was the following:

Table 1—Disturbance of 3 Mm. Arcs by Introducing Probes

Shield gas	*Probe diameter and material*	*Arc current (amp.) av.* Probe out	Probe in	Change, %	*Arc voltage (v.) av.* Probe out	Probe in	Change, %	*Arc power (watts) av.* Probe out	Probe in	Change, %
Helium	0.016 in. Chromel A	110	101	−8.2	19.4	21.3	+9.8	2130	2150	+0.9
Argon	0.016 in. Chromel A	103.5	102.9	−0.6	12.2	12.5	+2.5	1260	1285	+2.0
Argon	0.010 in. Chromel C	101.3	100.5	−0.8	12.5	12.8	+2.4	1270	1290	+1.6

A turntable of about 1-ft. radius which could be rotated at a series of speeds was set up and so arranged that two pieces of metal could be placed adjacent to one another on radial arms. The arc was started on the first piece and the turn-table rotated under it at a uniform speed until the second piece had passed completely under the arc. This piece was then immediately dropped into a well-lagged water calorimeter and the total heat adsorbed by the work was measured by measuring the rise in temperature of a known quantity of water. As the next step, the bead which was obtained was sectioned perpendicular to the weld direction, etched and the cross-sectional area of melted metal was carefully determined. From this quantity, together with the known speed of travel, the number of cubic inches of metal melted per kilowatt hour consumed in the arc could be evaluated. A large number of tests were conducted with this method to check the effect of such influences as sample size, shielding gas flow rate, arc length, welding speed, angle of electrode inclination, normal current density and total arc current on the results obtained. The range of these variables over which the results were reproducible was ascertained.

Table 2—Heat Transfer with Straight and Reverse Polarity

Arc length—0.050 in. Stainless steel—1/8 in. sheet
5/16 in. diam. electrode Argon flow rate—7 liters/min.
Welding speed—28 ipm.

D.-C. polarity	Straight	Reverse
Arc current, amp.	85	70
Arc voltage	11.4	16.4
Total arc power, watts	970	1150
Power to work, watts	750	780
Heat transferred, %	77	68
Melted metal area, sq. in.	0.0014	0.0016
Melted metal, cu. in./kwh.	2.4	2.3
Bead depth, in.	0.018	0.010
Bead width, in.	0.09	0.18

As an example of the utility of the above measurements, data are presented in Table 2 to show the relative heat transfer efficiencies of straight and reverse polarity in argon. It is seen that although the heat transfer efficiency for reverse polarity is somewhat lower, the volume of metal melted per kilowatt hour is essentially the same in both cases. However, it is noticed that the bead with reverse polarity is extremely broad and very shallow, not at all suited to most welding applications. Similar results were obtained for helium although here the quantity of metal melted per kilowatt hour decreased somewhat in going from straight to reverse polarity.

ULTRASLOW MOTION STUDY OF THE INERT-GAS-SHIELDED METAL ARC

Visual observation is probably the most elementary means of approach to any investigation, but is frequently restricted by the limitations of the eye. Even with adequate filters, the events and action taking place in welding arcs frequently cannot be followed by eye because of the rapidity with which they occur. For this reason ultraslow motion pictures have been made to help understand the mode of metal transfer existing under the different welding conditions. With motion picture cameras, such as the 16-mm. Kodak High-Speed Camera, action can be slowed down by a factor of about 200. Initially, this camera was employed for arc research, but it was found that higher camera speeds were desirable and all recent ultraslow motion work in these laboratories has been done with an 8-mm. Western Electric Fastax Camera, which normally permits time magnifications up to 500. By means of a Model J410 Fastax Control Unit, built by Industrial Timer Corp., the limit of picture taking speed of this camera can be extended to 14,000 pictures per second. This results in a time magnification of nearly 900 if the films are projected at 16 frames per second. Although the picture quality of 16-mm. cameras is normally superior, it was felt that the higher speed obtainable with 8-mm. cameras was necessary for this research work.

Details of High-Speed Camera

The operation of these high-speed motion picture cameras is somewhat different from that of conventional cameras in that the film moves continuously with no intermittent motion and no mechanical shutter. In the Fastax the film moves over a sprocket wheel located behind a rotating compensating prism which corresponds to the shutter of a conventional motion picture camera. As the prism rotates, (see Fig. 5), the image is formed by the lens at the film plane and moves in synchronism with the moving film so that a sharp image is produced.

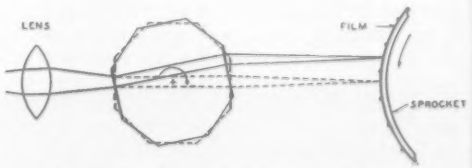

Fig. 5 Optical components of Fastax camera

At its highest speed, the passage of 100 feet of film through the camera will take less than one second and the exposure time at 14,000 frames per second is about 1/85,000 of a second per frame. With such short times, it is evident that very high light intensities are required for correct exposures. As the arc photographed for this investigation was of exceedingly high brilliance, auxiliary lighting was only required for special purposes. By having more than adequate light, another advantage ensued. Theoretically, the image behind the lens moves at film speed, but this is less exact above and below the center line of the image area. By cutting down the vertical apperture so exposure takes place close to the lens axis only, the

over-all image quality can be improved if sufficient light is available for correct exposure of the film. This modification was made on the Fastax camera used for most of this investigation and although the actual exposure time is not accurately known, it was assumed to have been about 1/100,000 of a second at a film speed of 14,000 frames per second.

Most high-speed motion picture cameras incorporate a means for determining the exact film speed at any portion of the film. On film exposed in the Fastax camera, timing marks produced by an argon glow lamp appear as streaks about $2^1/_2$ mm. wide along one edge and are located so as not to interfere with the picture area. With the frequency of the timing cycle known they form a simple means of determining the time between frames or the pictures per second.

Film Stock

Different types of film are, of course, available for use with these cameras. Films used in this work include black and white reversal, black and white negative, infrared and Kodachrome stock. In the limited tests made with infrared film it was not found to be of particular advantage and due to the difficulty of obtaining it, it has been relatively little used. Kodachrome film has been found extremely satisfactory, but has the disadvantage of being more expensive than black and white, and, like black and white reversal, it has to be sent out for processing, whereas negative stock can be developed with normal darkroom equipment. This is a great advantage, as test strips can be developed and checked for exposure a few minutes after the picture has been taken.

In taking motion pictures of consumable-electrode inert-gas-shielded-arc welding, it has been necessary to reduce the light intensity by means of filters so that correct exposures could be obtained within the diaphragm range of the lens. For this purpose neutral density filters varying from 0.5 to 3.0 (that is, transmitting from 30 to 0.1% of the incident light) have been used. A color correcting Type A filter was generally employed in conjunction with Kodachrome, as without this correction the Type A film showed a predominance of blue.

As is well known, color film has very limited latitude and the range of brightness of objects in the arc is far greater than can be recorded in a single exposure. Consequently a series of photographs of each arc condition with different filters or different lighting conditions is generally required for a complete study. It is largely for this reason that artist's drawings described below, have been prepared for this paper.

Procedures

Since the object of these studies has been primarily to investigate the type of metal transfer, it was felt essential that the full distance from electrode tip to bottom of weld pool should be visible in the pictures. Also, they should preferably be taken so that the particles would be traveling in a plane parallel to the picture plane, to facilitate measurement of distances and calculations of particle velocity. This was accomplished by producing welds on the top edge of plates which were narrow enough so that the bottom of the weld pool could be observed from the side. Although this condition is somewhat different from welding on a flat sheet with a deep weld pool, experience has not shown it to change arc behavior seriously.

Setting up and focusing the high-speed camera has to be done with considerable care to obtain best picture quality. For this reason, the camera and the arc were fixed in position and the workpiece was moved under the welding head. This had the added advantage of making it possible to make the exposure at any time during the duration of the welding run. For vertical welding, a similar procedure was used: by fixing the welding head in position, the filler wire or electrode was fed horizontally against the vertical portion of a rotating disk, resulting in effect in a vertical edge weld.

Some Qualitative Results

The greatest benefit derived from these motion pictures has been their contribution to a better understanding of the type of metal transfer taking place. This can only be properly appreciated by seeing the motion pictures themselves. It has not been possible to reproduce photographically enlargements from separate frames which satisfactorily illustrate all the features of interest. For this reason, artist's drawings have been prepared which faithfully represent some instantaneous conditions seen to exist repetitively under various conditions. All five drawings show three distinct zones which are common to these arcs. The outer zone is generally very faint and bell shaped. From observation of the base metal surface it does not appear to be nearly as hot as the intermediate zone since it often contacts solid material. The intermediate zone, which is always present and is always continuous from electrode to base metal, is apparently the main current carrying portion of the arc and also the heat liberating zone. Although it varies considerably in width and form, it, too, is generally bell shaped. Practically all melting of the workpiece takes place within its contact area with the base metal, and all cathode sputtering is confined to its contact with the negative arc terminal. The innermost, and most brilliant zone, is generally cylindrical, and does not appear to be necessary to the operation of the arc as it often disappears nearly completely, particularly in aluminum arcs. From observation of the motion pictures, it has been concluded that this zone contains luminous metal vapor which only appears when the electrode or metal particles have been heated sufficiently to vaporize the surface layer. Droplets in free flight are normally surrounded by a luminous vapor sheath which often precedes them down to the base metal, apparently repelled from the electrode tip as a comet tail is from the sun.

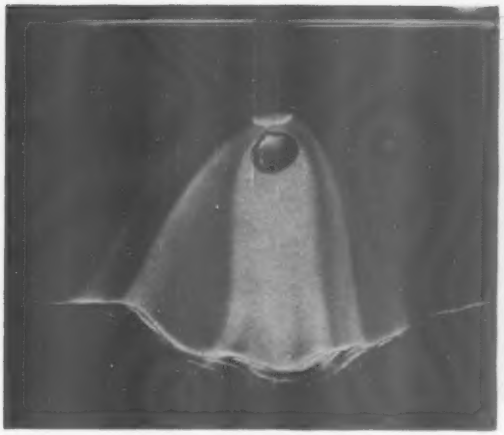

Figure 6 (A)

Figure 6 (B)

These drawings represent two phases of the metal transfer found to be typical during the welding of aluminum with direct-current reverse polarity in an argon atmosphere. There is seldom more than one particle in flight at one time. Cathode sputtering can be seen on the base metal

Under normal welding conditions, using direct-current reverse polarity, the unaided eye usually cannot detect separate particles transferring from rod to base metal. Through a welding shield the three arc zones can be discerned and the inner one can easily be mistaken for a zone in which metal transfers completely as a vapor. The motion pictures have shown that whenever metal transfer takes place it is almost completely in the form of discrete particles of diameters approaching that of the consumable electrode. The particles accelerate very rapidly as they are pinched off of the electrode end and then traverse the arc at substantially constant velocity. Normally there is no indication of shorting and, particularly with aluminum, there is generally only one particle in transit across the arc at any one instant. Figures 6 (*A*) and 6 (*B*), which are typical of aluminum welding with direct-current reverse polarity, show two instants in a particle's flight. In the first picture, the particle has just been severed from the electrode and has started on its free flight toward the work with a luminous metal vapor zone between it and the base metal. In the second picture, the particle has not quite reached the base metal. The luminous metal vapor zone has, however, followed along with it and, so far, the electrode has not been sufficiently heated by the arc to produce enough vapor to re-establish this inner zone, but a beginning of a new luminous vapor zone can be seen just beneath the rounded tip of the electrode.

If direct-current straight polarity is used with aluminum in amperages sufficient to cause rapid metal transfer with reverse polarity, it may only cause large drops to accumulate on the electrode tip. These eventually drop off mainly because of the influence of gravity. Figure 7 shows such a drop just before parting from the electrode. Cathodic sputtering is shown on the underside of the drop which behaves as though it were repulsed from the base metal by the arc action. This repulsive force of the arc is apparently overcome at higher currents than those used to produce Fig. 7, and rapid metal transfer can be achieved. However, the pictures of such metal transfer still show it to be considerably different than when reverse polarity is employed.

The metal transfer when welding steel at the common current densities employed with direct-current reverse polarity is somewhat different than that observed with aluminum. The most apparent difference is the fact that there are generally two or more particles in flight across the arc at one time and that the electrode tip becomes pointed, with the particles leaving the point as if being squirted from a nozzle. Figure 8 (*A*) depicts such an arc where four particles are all within the same inner arc zone. Although clearly visible as

Fig. 7 Illustration showing repulsive effect of arc on particle when direct-current straight polarity is used. High-current densities are required to overcome this effect. Note cathode sputtering on underside of particle

Figure 8 (A)

Figure 8 (B)

Typical metal transfer when welding with steel filler rod under good operating conditions in an argon atmosphere using direct-current reverse polarity. Figure 8 (*B*) shows how the luminous "metal vapor zone" is carried out of the arc center by a straying metal particle

separate particles in the pictures, the eye, even protected by welding glasses, can normally not discern them. If the arc becomes at all disturbed, one or more particles frequently leave the inner arc zone and it is then that it becomes apparent that with steel also, this zone is probably caused by the presence of luminous metal vapor presumably ionized. Figure 8 (*B*) shows such a straying particle.

Quantitative Results

From measurements on individual frames or sequences of them, it is possible to obtain considerable information such as exact size and velocity of particles transferring across the arc, as well as accelerations, kinetic energy and the forces involved. A few typical examples are given in Table 3.

One of the most interesting items in these calculations is the large force which is achieved in transferring particles across the arc space, a force which is many times that of gravity and which is considerably suppressed when straight polarity is used.

In conclusion, it is a pleasure to record our indebtedness to many co-workers who have contributed greatly to these investigations. We are also grateful to Professor Brian O'Brien of the Institute of Optics of the University of Rochester for much valuable advice and assistance with the slow-speed motion pictures and for modifications in the equipment which considerably improved picture quality.

Table 3

Rod material	Steel	Steel	Aluminum
Rod diameter, in.	1/8	1/8	1/8
Arc current, amps. (DCRP)	500	600	400
Arc length, in.	0.78	0.64	0.65
Rod speed, in./min.	80	100	100
Particles per second	543	707	293
Drop radius	0.035	0.027	0.035
Drop volume, $in.^3 \times 10^{-6}$	18	8	18
Mass per drop, lb. $\times 10^{-6}$	54	25	16
Av. particle vel., in./sec.	242	452	190
Final vel., in./sec.	484	904	380
Kinetic energy, ft. poundals $\times 10^{-6}$	4300	7000	730
Acceleration, $in./sec.^2 \times 10^6$	3	6	1
Force of free fall, poundals $\times 10^{-6}$	170	80	45
Total force, poundals $\times 10^{-6}$	120,000	140,000	14,000

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Spherical Shells Subjected to Internal Pressure and Low Temperatures

An interpretive report which discusses the results of a series of tests in terms of their apparent significance and possible utilization in engineering practice

by J. L. Walmsley

FOREWORD

A SERIES of tests in which spherical shell specimens were subjected to internal pressure up to rupture at subnormal temperatures was completed in 1948 under the direction of Prof. Joseph Marin in the Department of Engineering Mechanics at Pennsylvania State College. The project was sponsored by the Weld Stress Committee of the Welding Research Council for the purpose of determining the mechanical properties of a semikilled steel under conditions of biaxial-tension stress combined with moderately low temperatures. A report on this research, by Prof. Marin and his co-workers[1] appeared in the December 1948 issue of The Welding Journal, Research Suppl.

The purpose of the following summary report is to discuss some of the results of these tests in terms of their apparent significance and possible utilization in engineering practice, and to extend the conclusions in this regard as far as it seems expedient to do so in view of the limited number of tests. It is also desirable to tie in to some extent the results of the spherical shell project in proper relationship to those of the several more or less associated projects directed toward the problem of brittle fracture in welded structures.

Specifically the following points are discussed:

1. The nature of the fractures in the ruptured shells.
2. The bursting pressures and nominal ultimate strengths of the shells.
3. The plate ductility in the shells and the energy absorbed up to rupture.
4. The transition temperature of the material under "pure" biaxial stress.
5. The theory of ductility under multiaxial stress.
6. The theoretical ultimate strength of thin-walled shells.

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INTRODUCTION

Before discussing the results, a word on the background and nature of the tests will be in order.

When the program was authorized in 1944 and work initiated early in 1945, it appeared that tensile stresses of a multiaxial nature were contributing to the occurrence of brittle-type failures at low-nominal stress values in steel of commonly acceptable properties, particularly in the case of the much publicized Liberty ship failures, and a few instances of pressure-vessel failure. The case of simultaneous tensions in three perpendicular directions occurs only in very localized regions in the vicinity of notches, and this was, and still is, being studied for all practical purposes as a notch sensitivity problem. The case of biaxial tension on a larger scale, however, occurs very commonly in steel structures, and it is the prevailing stress condition in the great majority of pressure vessels.

The effect of biaxial tension stresses on the yield strength, ultimate strength and ductility of metals had received considerable previous study. With very few exceptions, however, all such previous work, that is, prior to 1945, had been performed on much smaller scale laboratory specimens (usually tubes) in which the material thickness was of the order of $^1/_{32}$ to $^1/_8$ in. thick. Thinness of section, both that to which it is rolled and that in which it is tested, frequently seems to be conducive to more ductile behavior. Also, a pronounced anisotropy of the material was often a disturbing factor in small tubular specimens as prepared from drawn tubing or bored from bar stock. Furthermore, this testing had been confined to room temperatures only, whereas low temperatures are known to be inimical to ductile behavior.

The spherical shell tests were designed, therefore, in conjunction with several similar projects [2,3] sponsored by government agencies, to study the behavior of a typical pressure-vessel steel, in more practical thickness dimensions, under conditions of biaxial tensile stress combined with subnormal temperatures. A total of six spheres were tested. A sketch of the specimens used is shown in Fig. 1.

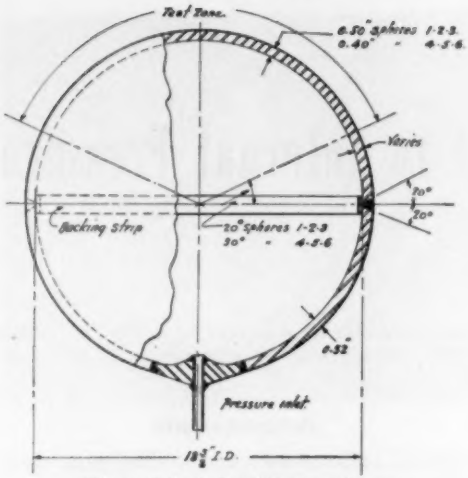


Fig. 1 Spherical shell-test specimens

NATURE OF THE TESTS

The material tested was a 0.23 C. semikilled steel conforming to A.S.T.M. Specification A70-44 (present equivalent A285-46, Grade C). This grade has been very commonly used for moderate-strength pressure vessels where welding was intended. Although not ordered to the same specification the sphere material was in fact quite comparable in both analysis and tensile properties (and approximately so in transition temperatures in notched bar tests) to project steel "A" as used in the tests on the tubular specimens[2,3] under the Ship Plate Program.

Fabrication of the spheres was from rolled plates, hot pressed into hemispheres of 7/8 in. thickness and machined to the dimensions shown in Fig. 1. A stress-relieving heat treatment was given before machining and after the final welding operation for all specimens. This heat treatment was used, not to improve the properties of the material to something better than the as-rolled plate, but for machining stability and to lessen the possible influence of the joints on the test results. The resultant tensile and notched-bar properties were determined on material taken from an excess rim on the hemispheres which received the same heat treatment.

The temperature at which the shells were tested was the only purposeful variable, the range of testing temperature being from +80° F. to −40° F. Prime consideration was directed toward the behavior of the material itself when not affected by obvious notches or conditioned by joint efficiencies or other complicating effects introduced by welded joints. In other words, it was felt that the fundamental properties of the parent steel itself should be more thoroughly understood for these stress and temperature conditions before we can evaluate properly the behavior of a composite structure. At the same time, it is expected that the tests may throw some further light on the probable factors of safety involved in present methods of design for this type of vessel—again in so far as these are governed by the properties of the steel and the shape of the vessel and not by the properties of the joints.

In this respect the sphere tests differ from the tests on the 18 1/2-in. diameter by 3/4-in. thick tubular specimens conducted by the University of California.[2] The latter tests, it should be remembered, were primarily tests of weldments where, in most cases, the results were conditioned by the strength and ductility of the welded joints and their orientation with respect to the maximum stress and do not necessarily reveal the more fundamental properties of the plate under combined stress conditions.

With the foregoing in mind, it was desired to have the fractures originate in the region marked "Test Zone" in Fig. 1, but at the same time to avoid any unnecessary influence from excessive reinforcement in other parts of the sphere. Initially, this test region was made 0.50 in. thick and other portions of the shell 0.52 in. thick. For the first sphere, tested at room temperature, the fracture originated in the test zone as desired, but in the next two tests at lower temperatures the fractures originated in welds and the latter two specimens, Spheres 2 and 3, should be considered separately. It then became necessary to reduce the thickness of the test zone to 0.40 in. for Spheres 4, 5 and 6, subsequently tested.

The testing thickness here is thus intermediate between that of the 1/4-in. thick small tubes,[3] and the 3/4-in. thick large tubes[2] utilized in the ship-plate program. This is not as thick, perhaps, as could have been desired, since it is possible that a net thickness of say 3/4 in. in the test zone would have constituted a somewhat more severe test of the plate ductility. This would have greatly increased the difficulties and cost of the program, however, and it is believed that conclusions based on the present results need be qualified only moderately for thicker service conditions in plates having similar as-rolled properties.

TEST RESULTS

For a description of the testing procedures, as well as a more detailed account of the resulting data and their manner of calculation, the reader will be referred to the investigator's report by Marin, Dutton and Faupel already published.[1] For purposes of easy reference in the following discussion, a summary of the principal results is given here in Tables 1 and 2.

Table 1 gives the tensile properties of the material as determined in the simple tension control tests on coupons from each of the six spheres. Each value shown is an average of about four tests. The tensile specimens were necessarily somewhat smaller than usual (0.357 in. diameter by 1-in. gage length), which may slightly influence some of the tensile properties shown. The tension tests, it should be noted, were all conducted at room temperature.

Table 1—Simple Tension Properties, Uniaxial Control Tests at Room Temperature

	Coupons from sphere No.					
Nominal properties	*1*	*3*	*4*	*5*	*6*	*Average*
Yield strength, (upper y.p.), psi.	44,000	37,700	40,000	36,500	40,000	39,600
Yield strength (0.2% offset), psi.	40,000	...	...	...	...	40,000
Ultimate strength, psi.	63,000	62,800	65,100	57,500	62,500	62,200
Fracture strength, psi.	50,300	51,100	57,500	45,900	50,100	51,000
Elongation in 1-in. gage length, %	32.8	...	...	...	...	32.8
Reduction in area, %	52.8	57.8	56.6	57.1	56.5	56.2
True properties						
True stress at ultimate load, psi.	78,100	76,700	77,400	75,800	77,500	77,100
True stress at fracture, psi.	113,400	121,500	122,000	111,700	118,800	117,500
True strain at fracture (zero ga. length),* in/in.	0.81	0.87	0.84	0.89	0.87	0.86
Specific energy absorbed at fracture, in lb./in.^{3}	70,900	...	73,500	73,800	77,000	74,000

* Based on reduction in area.

Table 2 summarizes the results of the tests on the spherical shells where the stress condition, of course, was essentially one of biaxial tension, of equal magnitude in all tangential directions, with a small compression in the thickness direction due to the internal pressure. Spheres 1, 6, 4 and 5, arranged in Table 2 in descending order of testing temperature, are mutually comparable in having the fractures start in the test zone where all measurements were taken. The observed properties of strength and ductility thus reflect the actual properties of the plate and were not controlled by reaching some prior limit in a joint as was the case with Spheres No. 2 and 3. It was not practical to conduct the tests slowly enough to avoid a gradual rise in the temperature of the shells during the course of the tests due to the heat generated in plastic deformation of the steel and other factors. The initial temperature at the start of straining and the temperature reached at fracture are listed, but, unless otherwise stated, the testing temperature hereinafter referred to will be the initial temperature of the steel when the deformation began.

The effects of the biaxiality of stress and subnormal temperatures on several properties of practical interest, as compared with the same properties in a simple tension test, may be shown more clearly by the graphical representations in Fig. 3. Before discussing the apparent significance of these results, however, it will be worth while first to examine the characteristics of the ruptured shells.

DISCUSSION OF THE RESULTS

1—Nature of the Fractures

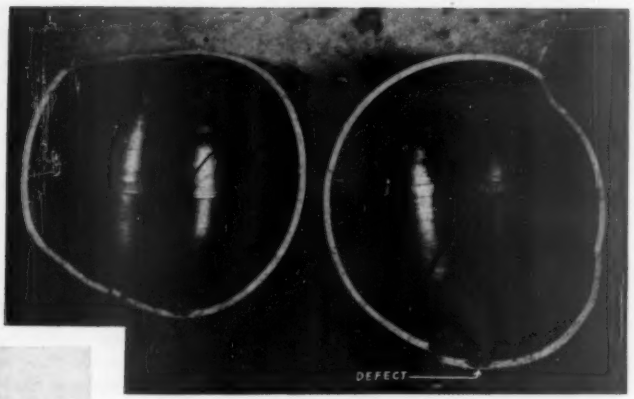
Photographs of the six ruptured spheres are shown in Fig. 2. Specimen 1, tested at room temperature, reached a maximum pressure of 5900 psi., at which time the elongation was only about 8%. After considerable further distension, under a gradually falling pressure, the rupture started near the top of the sphere and progressed to the extent shown in Fig. 2, entirely as a shearing fracture. That is, the fracture appearance has a dull fibrous texture and in this case, although it is not a necessary condition, the fracture surface is inclined at about 45° to the surface of the sphere. There is no tendency toward shattering with this type of fracture.

Specimen 2, next tested at a temperature of − 35° F., fractured suddenly at a pressure of 4730 psi. at a time when the pressure was still rising and when an elongation of only 2% had been reached in the test zone. This almost completely brittle fracture was of the cleav-

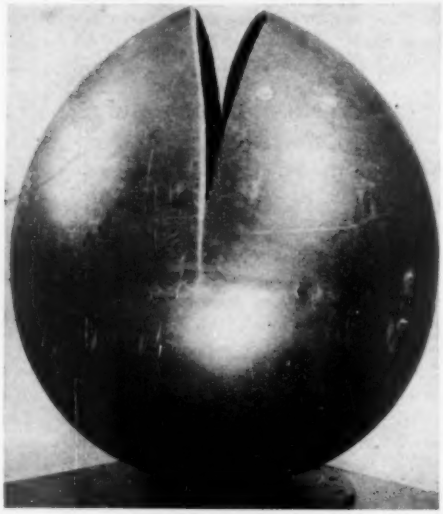
Table 2—Biaxial Tension Results, Spherical Shell Tests at Various Temperatures

	Spherical shell No.					
					Premature failures	
	1	*6*	*4*	*5*	*2*	*3*
Temperature of test {*Initial* ° F.	+ 80	+20	−20	−43	−35	−27
At fracture ° F.	+100	+60	+12	−25	−28	+16
Origin of fracture	Plate fracture in test zone				Plug weld defect	Girth weld
Mode of fracture	Shear	Cleavage			Cleavage	
Initial wall thickness, in.	0.50	0.40	0.40	0.40	0.50	0.50
Ultimate pressure, psi.	5,900	5,450	5,800	6,100	4,730	6,260
Comparable pressure for equivalent thickness, psi.	5,900	6,820	7,250	7,630	4,730	6,260
Nominal properties						
Yield stress, psi.	37,400	33,800	36,800	50,000	38,800	35,600
Conventional stress at ultimate pressure, psi.	55,000	63,500	67,500	71,100	44,400	58,300
Conventional stress at fracture, psi.	48,400	60,500	61,800	70,000	44,400	58,000
Per cent elongation in 2-in. gage length, %	31	24	19	27	1.9	5.0
Per cent reduction in thickness at fracture, %	51	42	34	45	...	...
True properties						
True stress at ultimate pressure, psi.	68,000	81,600	89,200	133,000	...	...
True stress at fracture, psi.	131,000	130,000	113,000	162,000	...	...
True strain at fracture (zero gage length),* in/in.	0.36	0.27	0.21	0.30	...	...
Specific energy at fracture, in lb./in.^{3}	65,100	52,600	37,900	64,600	...	...

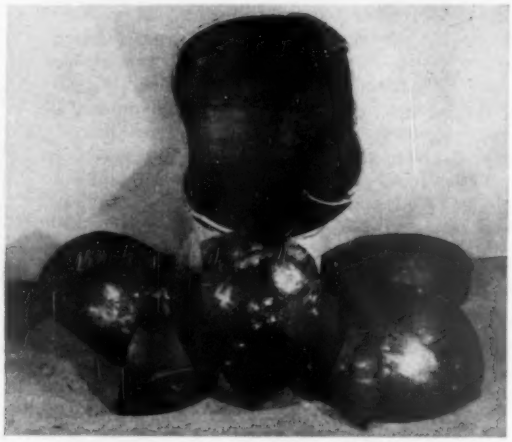
* Based on reduction in thickness.



Specimen 2 (*right*) Test temperature, − 35° F.; maximum pressure, 4730 psi.; cleavage fracture (originating at weld defect)



Specimen 1 Test temperature, + 30° F.; maximum pressure, 5900 psi.; shearing fracture



Specimen 3 Test temperature, − 27° F.; maximum pressure, 6260 psi.; cleavage fracture (originating in girth weld)

Specimen 4 Test temperature, − 29° F.; maximum pressure, 5800 psi.; cleavage fracture

Specimen 5 Test temperature, − 43° F.; maximum pressure, 6100 psi.; cleavage fracture

Specimen 6 Test temperature, + 20° F.; maximum pressure, 5450 psi.; cleavage fracture

Fig. 2 Photographs of fractured shells

age type, characterized by a brighter,* granular appearance and square-edged surfaces, with some shattering—that is, complete separation with several incomplete branches as shown in the photograph. Examination revealed a large subsurface defect in the weld joining the pressure inlet plug section, which is visible as the dark spot near the bottom of the photograph. Chevron markings indicated that the fracture had propagated from this defect; thus, nothing had actually been tested except a badly notched weld.

As a result of this experience, all subsequent specimens were fabricated under the most careful supervision including X-ray inspection of all welds. No further defects were found.

Specimen 3 was next tested at a temperature of $-27°$ F., and also fractured in a quite brittle manner on a rising pressure which, however, reached a value of 6260 psi. This, the highest pressure (but not the highest stress) reached in any test, caused the rupture to be particularly violent for this specimen. Elongations in the test zone had reached about 5%. The rupture was completely in the cleavage mode, and due to the number of completed branches the specimen shattered into a number of pieces as may be seen in Fig. 2. Chevron markings on the fractured surfaces indicated that the fracture had propagated by various branches from a single point in the circumferential or girth weld joint. There was no evident defect, however. But the results again were not controlled by the normal limits of strength and ductility of the plate material in an unnotched condition.

Reducing the thickness of the test zone to 0.40 in. had the desired effect and the fractures in Specimens 6, 4 and 5 originated in the test zones. These last three specimens, tested at $+20$, -20 and $-43°$ F., respectively, all passed through a pressure maximum, as had Specimen 1, followed by more or less further distension under a falling pressure until the rupture occurred. In Specimen 6 the initial 2 in. of the fracture was in the shearing mode, but the fracture was predominantly of the cleavage type and in final effect may be classed as such. Specimens 4 and 5 fractured completely in the cleavage mode. The amount of shattering may be judged from the photographs in Fig. 2.

Multiple branching fractures, as observed here, seem more likely to occur in spherical vessels where the stress ratio is 1:1 than in other circumstances where one stress predominates. It was noticed in the tests on large tubes at the University of California[2] and also in some of the older small tube tests (Maier, Ros and Eichinger) that the tendency toward multiple branching or shattering seemed more pronounced when the stress ratio was made 1:1 in the tubes. This does not necessarily mean that the metal is fundamentally more "brittle" under this ratio. A more logical explanation is that under the 1:1 ratio the fracture is equally free to take any direction where a weakness presents itself, whereas the possible directions are restricted to more narrow limits when one stress predominates. A high rate of propagation, characteristic of cleavage fractures, is prerequisite apparently.

The significance of the shearing and cleavage modes of fracture in the present tests may be stated as follows. With either mode of fracture the energy used in its propagation came entirely from the elastic strain energy recoverable through unloading the metal and the compressed oil used as a pressure medium.† For either type, this energy was a very small part (probably no more than a fraction of 1%) of the energy pumped in in distending the sphere to the rupture point. For either type, the progress of the fracture would be far too rapid in these tests to observe by ordinary means, even if the test were open to view. Nevertheless, a shearing fracture propagates slowly in comparison with the cleavage type, and absorbs a great deal more of the stored-strain energy. On the other hand, cleavage fractures once started may propagate so rapidly and with so little energy absorption that lengthy separation on one or more branches can occur before a considerable portion of the internal-strain energy is dissipated. The remaining energy is available for hurling the pieces.

The cleavage mode of fracture does not necessarily connote brittleness since, as will be shown, the ductility and energy absorbed prior to fracture may remain relatively good, and in the absence of notches the ultimate loading may increase at the temperatures necessary to promote this type of fracture.‡ If fracture must occur, however, it is evident that the cleavage type of fracture is potentially more dangerous from the standpoint of the extent of damage and possible injury to personnel attendant to the failure. This is particularly true of pressure vessels. Furthermore, while all cleavage fractures are by no means brittle, all brittle fractures are inevitably cleavage. Shearing fractures necessarily imply a large (although frequently localized) plastic deformation that is sufficient to remove most of the stress concentrations at ordinary internal and external notches and, hence, simply do not occur in properly designed structures except by gross overloading.

2—Ultimate Strengths and Pressures

The ultimate pressure observed in each of the six spherical shell tests is given in Table 2. The ultimate in the room-temperature test was about 87% of the pressure that might be expected on the basis of a conventional calculation using the tensile strength of the material and the initial dimensions of the sphere. The effect of temperature is not immediately apparent from a comparison of the ultimate pressures because of the two different wall thicknesses involved, but an

* When the fractured surface is held in a horizontal position and viewed with frontal lighting. With over-the-shoulder lighting the cleavage facets reflect little to the eye and the relative shades may appear reversed.

† Compressed gases can store larger strain energies.

‡ Similar observations were made in the small tube tests by Griffis.[2]

equivalent pressure for equal thicknesses is also shown in this table. Except for specimens 2 and 3 which were controlled by joint failures, these equivalent pressures indicate that with the same 0.50 in. wall thickness the ultimate pressures would have increased continuously as the testing temperature was lowered.

Comparing bursting pressures in this manner is really equivalent to comparing the nominal ultimate strengths in the shells since the quantities are proportional. In Fig. 3 (*a*) the ultimate strengths as conventionally calculated are plotted against the testing temperature for spheres 1, 6, 4 and 5. The flag marked T.C. on this diagram locates the tensile strength of the material as obtained in the tensile control tests in relation to the strength of the shell at room temperature. The ultimate strengths of the shells show a small but definite and almost linear upward trend with decreasing temperature.

This ultimate strength trend in the shells reflects the normal effect of temperature on the flow stress or resistance to plastic deformation in the steel. These circumstances, however, should be distinguished from those in which a drop in breaking stress may be experienced at low temperatures, particularly in sharply-notched specimens, when the maximum load is the fracture load. The uniaxial tensile strength of the present material was determined only at room temperature. However, some idea of the effect of temperature on the ultimate strength in simple tension may be obtained from some tests on mild steel by Colbeck as reported by Gillett.[5] This is represented by the dashed line in Fig. 3 (*a*). It is of interest to note from this diagram that the nominal ultimate strength in the shells closely parallels the probable simple tension trend in this temperature range. The reasons why the nominal ultimate strength of a ductile shell may be below the tensile strength of the material will be discussed from the theoretical point of view in Section 6.

3—*Ductility and Energy Absorption*

One of the questions that these tests were expected to answer more definitely was—can the approximately 0.20 C semikilled steels be made substantially brittle by pure biaxial tension at cold weather temperatures? By pure biaxial tension we mean without the addition of notch effects. The results shown in Fig. 3 (*b*), (*c*) and (*d*) indicate that the answer is no for temperatures down to about − 40° F. There is an irregular tendency toward less ductile behavior at the lower temperatures, however.

The effect of the biaxial stress on the ductility depends upon what quantity is used as a measure of ductility. Fig. 3 (*b*) shows the per cent nominal elongations, as measured in the test zone after fracture and referred to the original 2-in. gage length. The flag marked T.C. indicates the tensile control test value.

On this nominal basis there would appear to be little difference at room temperature between the biaxial and uniaxial elongations. The near agreement of these values may be partly incidental (it may be influenced by the gage lengths and strain distributions, particularly in the tensile specimens) but it is indicative that biaxial stress in itself does not cause any pronounced change in the elongation of this material as averaged over an arbitrary gage length—at least at normal temperature. Quite possibly this reflects an absence of any very radical change in the uniformly dis-

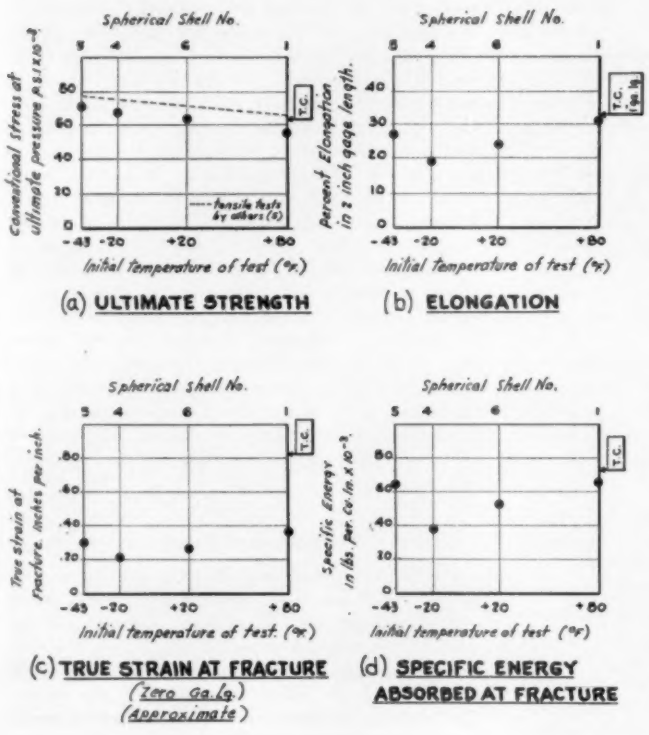


Fig. 3 Results of spherical shell tests at various temperatures: (a) ultimate strength; (b) nominal ductility; (c) true strain at fracture (zero ga. lg.) and (d) specific energy absorbed

tributed part of the strain[§] because, as will appear, there is a very large difference between the maximum local extensions at the point of fracture in the two types of test.

At lower testing temperatures, the nominal elongations in the shells dropped off to lower values—in one case to as little as $^5/_8$ of the room temperature value. This is undoubtedly cause for some caution, but even this minimum elongation of 19% in the test at − 20° F. does not indicate a dangerous approach to brittleness in this temperature range—particularly when the test at still lower temperature (− 43° F.) gave the second highest elongation. Caution is needed in applying this conclusion to more ordinary construction because it must be kept in mind that additional embrittling factors, which are likely to be present at every kind of joint, have been carefully excluded from the present test conditions.

If we consider the maximum local extensions in a hypothetical zero gage length at the point of fracture, the effect of biaxial stress appears somewhat different. This is illustrated in Fig. 3 (*c*), in terms of the true strains at fracture as calculated from the reduction of area in the tensile test and from the reduction of wall thickness in the sphere tests. For the tensile control tests the maximum positive true strain, computed according to the definition

$$\mathbf{E} = \log_e \left(\frac{A_0}{A_f}\right) \qquad \text{(a)}$$

—where A_0 and A_f, respectively are the original and final areas at the fracture cross section—has a value of 0.81 in. per in. (average) for the tests on material from Sphere 1. The corresponding quantity in the spherical shell tests is defined by¶

$$e_1 = {}^1/_2 \log_e \left(\frac{t_0}{t_f}\right) \qquad \text{(b)}$$

where t_0 is the original wall thickness and t_f is the thickness measured at the fracture cross section. For the room temperature sphere test, this quantity has the value of 0.36 in. per in. The latter is a considerably less "ductile" value in the sense that the positive strain or extension has now been divided between two directions although the amount of deformation undergone in terms of shearing strain was almost as much as in the simple tension specimen. For example, the maximum shearing strain (the algebraic difference between the greatest and least principal strains) was 0.81 − (− 0.40) = 1.21 (average) for the simple tension tests on material from Sphere 1, as compared with 0.36 − (− 0.72) = 1.08 minimum approximate value under the biaxial stresses in the sphere test. It should be mentioned here that while the fracture strains were not distributed with entire uniformity, no local necking was observed in the spheres of a nature calculated to upset the approximate equality of either the stresses or the strains in the circumferential directions.

In any case, we are dealing here with quite large strains. Hence, for most engineering purposes it is presently a matter of only secondary interest that at normal temperature the observed values of true strain and reduction of thickness at fracture under biaxial stress agreed reasonably well with the values predicted by theoretical analysis from the simple tension test results. It is not possible to make comparisons between the theory and the tests at lower temperatures for reasons which will be discussed briefly at a later point.

A further property directly related both to the ductility and the stress level is the energy absorbed prior to fracture. The specific energies absorbed up to rupture in the spheres are shown as a function of the testing temperature in Fig. 3 (*d*), with the corresponding value determined from the tensile control tests again indicated on a comparable basis. These values represent the amount of energy absorbed per unit volume of metal by an element located at a point on the fracture cross section, and were determined from the areas under the "significant" stress-strain curves for the various specimens (see Fig. 20 of Prof. Marin's report[1]). In terms of the three principal stresses and strains for the element, this is equivalent to the summation

$$U = \int \sigma_1 \, de_1 + \int \sigma_2 de_2 + \int \sigma_3 de_3 \qquad \text{(c)}$$

The energy absorbed by the specimen as a whole, customarily measured when using identically notched specimens, would not be a suitable basis of comparison between dissimilar specimens of course.

Referring to Fig. 3 (*d*), there appears to be a relatively minor difference at room temperature between the specific energy absorbed in the tensile control tests and in the spherical shell test—the latter value being slightly the lower. In fact, the difference may be due in part to the error in neglecting the presence of the transverse tensions in the necked section of the tensile specimens.** One test (at room temperature) is not enough to prove the validity of the theory, but it is again of some interest for further reference to note that the plastic stress-strain relations for isotropic materials as outlined in Prof. Marin's report,[1] with the assumption of a critical shear stress criterion for fracture, would also require the value of the specific energy absorbed in the sphere (i.e.,for any stress ratio of the form $\sigma_1:\sigma_2:\sigma_3 = 1:1:\beta$) to be the same as in uniaxial tension. It should be mentioned, however, that quite different results have been obtained in some of the tests at a 1:1 stress ratio in small tubular specimens[3, 4] where anistrophy of the material appears to have been an important if not the controlling factor.

§ This would accord with the theory that the uniform part of large strains in isotropic materials is not a function of the initial-stress ratio. In this connection it should be mentioned that maximum pressure in a pressure vessel marks a mechanical instability but not the end of homogeneous strain as does maximum load in a tensile test.

¶ This defines an approximate value based on the assumption that the biaxial extensions occur symmetrically or in equal magnitude under the 1:1 stress ratio. If they are not exactly equal the larger principal strain may be somewhat greater but in no case can be less than the value so calculated from the thickness measurements.

** For the tensile test, the second and third terms of equation (c) are usually assumed to be zero although they probably should have small negative values.

Considering now the tests at lower temperatures, it will be seen from the diagrams in Fig. 3 that the nominal elongation, true strain at fracture and specific energy absorbed all show a fairly similar pattern of results when plotted against the initial temperature of test. (The same would be true for the final temperatures at fracture.) We have refrained from drawing curves through these points in an attempt to represent the influence of temperature on these properties in a more quantitative way because it was felt that the number of tests is not sufficient for this purpose. The difference between the behaviors of spheres 4 and 5, for example, is indicative of chance factors, and while it seems likely that Specimen 5 represents an abnormally high result it is also possible that Specimen 4 was an unusually low one. Adopting the more conservative view that Specimen 5 was quite abnormal, it is still evident that under this stress ratio the material does not exhibit a transition temperature with respect to ductility and energy absorption for temperatures down to at least − 40° F.

4—*Transition or Brittle Temperatures*

The four sphere tests in which fractures were obtained in the test zone are not sufficient to determine definite transition temperature values for the biaxially stressed material on either a ductility or a mode of fracture basis. However, it is possible to point out some approximate limits as a means of indicating the general position of the biaxial case (in this particular plate thickness) in relation to the transition temperatures of the same material as determined in two types of notched-bend tests.

The transition temperatures of the material used in the spheres were determined in both standard Charpy key-hole notch impact tests and in V-notched slow-bend tests of the Penn-State[6] type. These temperatures and the criteria by which they were defined are shown in Table 3.

Table 3—Approximate Relation of Transition-Temperature Range in Spherical Shell Tests with Respect to those in Notch-Bend Tests

Type of test	*Ductility or energy absorption criteria*	*Transition temperature ° F.*	*Transition temperature ° F.*	*Mode of fracture criteria*
Slow v-notch bend tests	50% Reduction in lateral contraction	+43	+43	50% Cleavage
Standard Charpy K. H. notch-impact tests	20 Ft.-lb. energy absorbed (about 50% reduction)	0	+40	First appearance of cleavage
Spherical shell tests	50% Reduction in ductility or energy absorption	(?) Below −40	Above +20 Below +80	50% Cleavage

As a rule, the ductility and energy absorption criteria seem to be concerned largely with the initiation of the fracture—that is, the magnitudes of the properties to be measured are substantially determined by the time the fracture begins its run—and transition temperatures based thereon are thus quite sensitive to differences in the geometry of the specimen and the resulting constraint or stress system near the point of origin. Thus, on the usual basis of a 50% reduction in the ductility or energy absorption, the case of biaxial stress with no superimposed notch or welding effects may be placed, as a first approximation, at somewhere below the lowest sphere test temperature (− 43° F. initial, − 25° F. at fracture) for the present material and wall thickness. As indicated in Table 3, this is at least 40° lower in transition temperature than in the Charpy specimen and at least 80° lower than in the Penn-State specimen. (We are speaking arbitrarily of the initial temperatures since the deformation is partially adiabatic in the Charpy test as well.) The state of stress at the root of a notch, then, is a much more potent factor in raising the brittle temperature than is pure biaxial stress alone.

On the other hand, transition criteria based on the mode of fracture appear to be concerned to a much lesser degree with the circumstances under which the fracture originates but simply with the manner of its ultimate propagation. The progressing fracture is a natural notch in which the effective constraint depends appreciably upon the thickness of the material and may be only remotely related to the original stress ratio in the plane of the plate. Since this fracture notch is introduced into prestrained material (bringing with it, in many cases, a high local strain-rate from the stored strain energy) it seems a very natural result to find the type of behavior exemplified by spheres 4, 5 and 6, which could be described as fracture brittle but pre-fracture ductile.

By the criterion of the predominance of shear or cleavage in the fracture, Sphere 6 would be said to have been tested at below its transition temperature and Sphere 1 at well above. In terms of the initial temperatures of the specimens this would place the fracture transition for these test conditions at somewhere above +20° F. but below room temperature; hence, it is probably within ±20° of, and may be very little different from the value indicated by the V-notch slow-bend specimen as given in Table 3. This range is also approximately comparable to that usually obtained with the Kahn Tear Test in which the stress is nominally uniaxial. When allowance†† is made for the relatively small wall thickness (after machining) of the shells, however, it remains distinctly possible, and even probable, that this fracture transition would have occurred at temperatures at least as high as room temperature if the original rolled thickness (7/8 in.) could have been retained in the spheres. [In this connection it may be

†† The mechanical effect of thickness (excluding metallurgical factors) upon fracture characteristics has not been investigated in this type of test but was studied to some extent in 3-in. wide-edge notched tensile bars in the ship plate investigations.[7]

noted that the present material appears to correlate rather closely with the transition temperatures reported[6] for the semikilled ship steel "A" in the same notched-bend tests, and that steel "A" as tested in the large $^3/_4$-in. thick welded tube specimens[2] gave predominantly cleavage fractures at room temperature. In comparison, the fracture transition in the 72-in. wide centrally-notched flat plates of the same material and thickness was placed at about +40° F.[7] Whether the higher transition of the tubular specimens was caused solely by the biaxiality of the stress or perhaps in part by other factors such as fabrication effects, possible differences in the potential strain energy developed, opportunities for strain aging, etc., cannot be conclusively stated.]

Although we cannot be very specific in regard to the exact temperature at which the mode of fracture changes, we clearly must expect that the operating temperatures for unfired pressure vessels in many types of service will more or less often lie below the mode of fracture-transition temperature for this or similar material under the conditions of use in those structures. That is, assuming that a rupture must occur at what may be regarded as normal winter-season temperatures, irrespective of whether the load required to cause the failure may be low or exceptionally high it follows that the fracture is quite likely to be of the cleavage and possibly shattering type. While it is well to recognize that this situation exists, it does not seem to be one of recent origin, nor would it appear to be confined to structures of welded fabrication. It is probably true of a great many structures giving satisfactory service today. Improvements in this respect are known to be obtainable through the greater use of fully-killed steels, grain-refining heat treatment and lower carbon, alloy steels where warranted by the importance of the structure and the severity of the service. But for the immediate future, at least, it appears that it may prove economically impractical to avoid this situation altogether in the case of the larger average duty structures.

This brings up the question: Is the safety of a structure to be considered as impaired if it must operate at a temperature below the fracture transition temperature of the component material? Inasmuch as service failures by shearing fracture have been virtually unknown within the expected load capacity of competently designed structures, from a purely statistical point of view the answer must be yes. On the other hand, we may expect that the load required to cause a fracture will be higher than normal under these same conditions except in so far as this expectation may be modified by high stress concentrations at defects or other discontinuities accompanied by a lack of notch ductility. On the basis of the present tests it appears that the key to the behavior will ordinarily lie in the joints or in some attached appurtenance.

For example, as indicated in Table 2, cleavage fractures occurred in all spheres tested at +20° and below, and with the exception of Sphere 2 containing the weld defect these spheres all sustained higher ultimate pressures (for equivalent thickness) than did Sphere 1. (Actually, of course, there was no connection between the ultimate load and the subsequent mode of fracture in any of these tests except the two cases—spheres 2 and 3—where the fractures originated in the joints.)

The propensity for cleavage fracture within the probable range of operating temperatures may be regarded, therefore, as a requisite but insufficient condition for unsatisfactory load capacity, and we may conclude that under service conditions such as simulated by these tests the soundness and/or notch-ductility of the joints and attaching details will be of paramount importance in determining the actual performance of the structure.

The question of the entire suitability of this material for biaxial stress service at subnormal temperatures thus may be said to revert, essentially, to a matter of its notch sensitivity and/or "weldability" including the effects of a number of fabrication variables. An attempt to state proper standards of material suitability in this respect would be beyond the scope of the present report, and must be left to a continuing series of research programs being conducted under the sponsorship of the Weldability and Pressure Vessel Research Committees of the Welding Research Council, as well as the Ship Structure Committee and the Office of Naval Research. Progress reports on this work are appearing from time to time in the Welding Journal, and a Monograph on the subject of Weldability is due to be published shortly.

The sphere test results do show that a notch effect such as may result from defective workmanship in a joint can be the nucleus for initiating a very brittle fracture in a biaxially-stressed structure at moderately low temperatures. Whether such a notch or flaw is a greater potential hazard than it would be in uniaxially-loaded plates of similar size at these temperatures has not been sufficiently established, however, If further tests of this character are carried out in a future program, it would seem desirable to study the effect of reproducible discontinuities in both uniaxial and biaxial stress fields.

CONSIDERATION OF THE THEORY

Excluding the contingency of premature brittle fracture associated with low-temperature notch effects, and assuming 100% joint efficiencies, the test results from Sphere 1 afford a partial check on certain theories of strength and ductility for biaxially-stressed structures. The results indicate that the theoretical stress-strain relations connecting the tensile-test data with the behavior of the material under balanced biaxial stress at room temperature may be said to hold with a fair degree of accuracy for the material tested. This brings us to the question of the probable utility and limitations of these theories for engineering use.

5—Theory of Ductility

A valid theory of ductility under multiaxial stress

conditions has long been sought for two reasons:

1. As a means of foreseeing from tensile test results the probability of the occurrence of brittle fracture under a given combined stress condition, because structural failures have generally been associated with brittleness.

2. As a means of forecasting large local deformation limits in the cold forming of metals.

Both (1) and (2) involve fracture under quasi-static loading and a rational theory of ductility hence must combine a theory of flow with a theory of fracture strength.

With respect to (1), little progress has been made, and it may have but very little point except for materials of naturally low ductility in the tension test. For example, the spherical shell-test results, as well as the many tests on tubular specimens, have shown that when the local effects of welding practices or other notch effects do not intervene, the approximately 0.20 C semikilled steels under biaxial tension stresses will at normal temperatures retain substantially all of their uniaxial capacity to deform and at moderately low temperatures at least a substantial portion thereof. (We say to deform rather than to extend since to equal the local uniaxial extension in two principal directions simultaneously would mean asking the material to undergo about twice the shearing strain and absorb more than double the work absorbed in the tensile test.) It appears that in order to produce near-brittle fractures in such material we must either go to extremely low temperatures or introduce notch effects whereby we lose an authentic knowledge of the controlling stress conditions.

The present discussion is confined, therefore, to the influence of the stress ratio on comparatively large strains. These may or may not become more or less localized (provided the calculated stress ratio is not too much upset thereby) and for theoretical purposes it is necessary to treat ductility in terms of the true strains at the point where the limiting strain is reached, i.e., in an infinitesimal gage length at the fracture. Accordingly, the term "ductility" is here applied to the limiting value of the true strain in the direction of the maximum principal tension—at the fracture (the quantity plotted in Fig. 3 (*c*). The other two principal strains may be as easily determined, however.

In their report,[1] Prof. Marin and his collaborators presented a rational theory based on certain postulates of established precedent which, although almost never completely fulfilled for technical materials, have been, nevertheless, confirmed as useful approximations. These can be summarized as follows:

(*a*) For all (approximately constant) sets of ratios between the principal stresses, the "significant stress" vs. "significant strain" relations, throughout the deformation up to fracture, coincide as a single curve identical with the true stress-true strain relation for simple tension, and may be represented by an equation of the form:

$$\sigma_s = k\epsilon_s^n \qquad (1)$$

(*b*) The terminal point of the above relation (Equation 1) for any given ratio of the principal stresses (including the case of simple tension) is determined by reaching a critical value of shearing stress (or alternatively a critical value of tensile stress) as the assumed criterion of rupture.

In Equation 1, σ_s is the significant stress, ϵ_s the significant strain, and k and n are material constants. The only quantity in this equation that need be used directly, however, is the strain-hardening exponent n, which may be evaluated from the simple tension test as the slope of the best linear relation between true stress and true strain in a log-log plot. Its (dimensionless) value normally appears to fall between about 0.20 and 0.30 for as-rolled or annealed mild steel but it may decrease to considerably lower values for high strength steels.

Included in (*a*) above, are a number of subsidiary assumptions which may be considered as automatically valid as long as (*a*) is approximately true. Among these is one of isotropy of the material. There is some evidence that the relatively heavy spherical shell plates may come somewhat nearer to the assumed properties of an idealized material in this respect than has often appeared to be the case in tests of small scale tubular specimens made from drawn tubing or rolled bars, and this may hold true for other products that have undergone a more extreme reduction from the ingot stage.

The assumption of a critical shear stress as the criterion for fracture where the separation starts in the shearing mode and a critical tensile stress as the criterion for fractures starting in the cleavage mode seems to be the best available, although the deficiencies of so simple a rule are frequently apparent.‡‡ For fracture under biaxial tension stresses where the third principal stress is of comparatively negligible magnitude, as is commonly the case in thin-walled pressure vessels or diaphragms, and in some bending and forming operations, the choice between a critical shear stress or a critical tensile stress as the criterion for rupture is of no practical consequence—the theoretical difference in ductility being less than the probable error from other causes. On the other hand, various tests under combined stress in which the minimum principal stress (i.e., the maximum negative stress) has been of a more influential magnitude, appear to indicate the shear stress criterion to be a more generally applicable assumption as long as the separation occurs in the shearing mode. (The spherical shell tests do not enlighten us very much on this point.)

The theory, involving functions of the stress ratios only, evidently should not be presumed to be applicable over a range of conditions in which the mode of fracture changes without due regard to the existence of a probable discontinuity in the condition for rupture; nor

‡‡ It is doubtful that either criterion can be expected to hold over more than a limited range of essentially biaxial stress conditions. This arises in part from the fact that the resistance to fracture seems to be a function of the strain itself.

over a range of stress conditions where the temperatures are not reasonably constant—that is, the material constants should be known for the same temperature as that of the combined stress case under consideration. Where the material constants are to be based on a simple tension test, it is of interest to note in this connection that a series of tests on unnotched 0.505 in. diameter tensile samples of three semikilled ship plate steels[8] appear to indicate that cleavage fractures may not be expected in standard 0.505-in. diameter tensile specimens of mild steel until temperatures of around − 200° F. are reached.

With the above prefacing remarks, and assuming a critical shear stress criterion for fracture, the theoretically predicted[1] value of ϵ_1, at rupture, designated as ϵ_{1r}, may be written conveniently for the general case in the following form

$$\epsilon_{1r} = \frac{1 - {}^1/_2(\alpha + \beta)}{\psi}\left[\frac{\psi}{1 - \beta}\cdot\frac{2\tau_{cr}}{k}\right]^{\frac{1}{n}} \qquad (2)$$

in which the principal stress ratios are designated by

$$\alpha = (\sigma_2/\sigma_1) \text{ and } \beta = (\sigma_3/\sigma_1) \qquad (d)$$

and the significant stress ratio by

$$\psi = \frac{\sigma_e}{\sigma_1} = \sqrt{\frac{(1-\alpha)^2 + (\alpha - \beta)^2 + (\beta - 1)^2}{2}} \qquad (e)$$

n and k are, respectively, the strain-hardening exponent and the strength coefficient appearing in Equation 1, while τ_{cr} is the critical shear stress required for fracture referred to the effective cross section at rupture.

It will not be necessary to know the actual value of this fracture stress, however, since we may utilize the ductility as measured after fracture in the tension test (Equation a) as a material constant replacing both τ_{cr} and k. That is, for simple tension, where $\alpha = \beta = 0$ and $\psi = 1$, the right hand side of Equation 2 reduces to the term $[2\tau_{cr}/k]^{1/n}$. Hence, in its simplest form for practical use the theory may be expressed as a ratio of the ductility (ϵ_{1r}) in the general case to the ductility (ϵ_{0r}) in simple tension, thus:

$$\frac{\epsilon_{1r}}{\epsilon_{0r}} = \frac{1 - {}^1/_2(\alpha + \beta)}{\psi}\left[\frac{\psi}{1 - \beta}\right]^{\frac{1}{n}} \qquad (3)$$

When $\alpha = 1$, corresponding to the case of two equal tensions as in the spherical shell tests, $\psi = 1 - \beta$ and the ductility ratio as given by Equation 3 becomes, as a particular case, a constant value of $^1/_2$, independent of the values of β and n and, thus (theoretically) independent of the material. For the test on Sphere 1, the observed ratio was

$$\frac{\epsilon_{1r}}{\epsilon_{0r}} = \frac{0.36}{0.81} = 0.45$$

As in many cases of essentially biaxial tension stresses ($0 < \alpha \leq 1$), if β may be considered as negligibly small in comparison with unity, a simplification more suitable for graphical representation is possible in that when $\beta = 0$, Equation 3 may be written as follows:

$$\frac{\epsilon_{1r}}{\epsilon_{0r}} = (1 - \alpha/2)(1 - \alpha + \alpha^2)^{\frac{1-n}{2n}} \qquad (4)$$

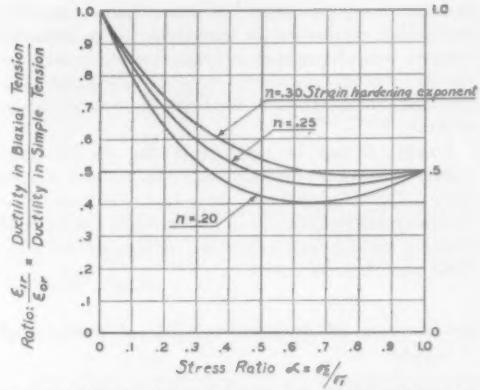


Fig. 4 Theoretical ratio of ductility under biaxial tension stresses to ductility in simple tension test. (Maximum strain at point of fracture)

The theoretical variation of the ductility ratio $\epsilon_{1r}/\epsilon_{0r}$, as given by Equation 4 is plotted in Fig. 4, as a function of α for several values of the strain-hardening exponent n.

Where a critical tensile-stress criterion for fracture seems to be the more applicable (this is difficult to prove but it is probably limited to cases where the fracture starts in the cleavage mode) the theoretically predicted value of ϵ_{1r} at rupture under combined stress is

$$\epsilon_{1r} = \frac{1 - {}^1/_2(\alpha + \beta)}{\psi}\left[\psi\frac{\sigma_{1cr}}{k}\right]^{\frac{1}{n}} \qquad (5)$$

where σ_{1cr} is the critical tensile stress required for fracture. Assuming this fracture criterion to hold for simple tension, the ductility ratio becomes

$$\frac{\epsilon_{1r}}{\epsilon_{0r}} = \frac{1 - {}^1/_2(\alpha + \beta)}{\psi}[\psi]^{\frac{1}{n}} \qquad (6)$$

Comparing Equations 6 and 3 it will be seen that as β approaches very small values the two equations become virtually identical. Therefore, for cases of essentially biaxial tension stresses, Equation 4 and Fig. 4 should be equally applicable for either criterion of fracture.

The spherical shell test results obviously provide only a very limited check on the validity of the theory and further confirmation is necessary before its utility can be established—particularly for steel. In principle, the theory is basically identical with that presented by Dorn and Thomsen[9] who found a fair correlation between the calculated and experimentally observed strains at fracture for several nonferrous metals. On the other hand, the biaxial stress tests by Griffis,[3] using small tubular specimens bored from mild steel bars, point up a serious difficulty in way of developing a useful theory of ductility under combined stress. For example, assuming an isotropic material, the theoretically predicted value of ϵ_{1r} at rupture under balanced biaxial tension was stated above as $^1/_2$ the simple tension value. For the tubes used by Griffis, the simple tension values for the longitudinal and transverse directions differed by something like a factor of 2,

and for the biaxial cases no more than a roughly qualitative correlation is discernible. For material wherein such directional differences exist, it may be doubted that any simple theory can be very accurate in predicting the fractures strains under general conditions of stress.

Finally, it may be emphasized that we presently appear to have no rational analytical method of predicting the threshold conditions that will cause substantially brittle behavior in mild steel on the basis of ordinary tension-test results involving large deformations and shearing fractures.

6—Theories of Strength—Nominal Factors of Safety

In this section, we would like to examine the apparent relation between the ultimate strength of a thin-walled pressure vessel at normal temperature and the tensile strength of the material—that is, in so far as this strength relationship may be governed by the mechanical properties of a ductile material and the general shape of the vessel, and not by matters of joint efficiency or details of the design.

As was previously mentioned in the discussion of the test results, the nominal ultimate strength obtained in the room temperature test on Sphere 1 was about 87% of the tensile strength of the control specimens taken from the same shell. Although this is but a single test, there are a number of individually inconclusive indications that this was not just an accidentally low value but was probably a rather normal result. First, it was associated with a true pressure maximum occurring long before rupture, and is less likely to be sensitive to the minor material variations to which fracture observations are subject. Second, Fig. 3 (*a*) indicates that all of the spheres in this series that reached a true pressure maximum were probably consistent in this nominal strength aspect, although tensile tests were not made at the lower temperatures. Third, in the room temperature tests of mild-steel tubular specimens, the ultimate strengths as reported by Griffis[3] were relatively even lower for the 1:1 stress ratio in small unwelded tubes. The circumferential stresses at ultimate load in the large welded tubes tested under the ship-plate program[2] also were always lower than the transverse tensile strength of the material (about 90% thereof) but in some cases this may have been limited by premature rupture. A 72-in. diameter mild-steel spherical vessel tested to destruction (after some battering) in 1929[10] also appears to have developed a nominal ultimate strength of about 90% of the original tensile strength of the material. The small alloy-steel tubes tested by Sachs and Lubahn[11] are an exception that will be referred to again later. Finally, careful analysis of the mechanics of the internal pressure instability condition in a shell, assuming an idealized, isotropic material of the present strain-hardening characteristics, seems to indicate that the maximum pressure in the spherical shell test was most likely to occur at a nominal stress of just about the value observed, provided premature fracture did not intervene as a lower limit.

When the thickness to diameter ratio of a shell is as small as in the present case ($t_0/d_0 = 1/37$), we may for nearly all practical purposes, in either the elastic or plastic stage, consider the principal tensile stresses to be uniformly distributed across the thickness and use the average value. Analogous to the conventional tensile strength in the tensile test (ultimate load divided by original area) the nominal ultimate strength of the sphere is then defined by the conventional thin-wall formula using the initial dimensions of the shell

$$\sigma_{1u}{}' = \frac{p_u d_0}{c\ t_0} \tag{7}$$

where $\sigma_{1u}{}'$ is the nominal circumferential stress at ultimate pressure, p_u is the ultimate pressure, d_0 is the original (inside) diameter and t_0 is the original wall thickness; c is the geometrical factor of 4 for a sphere or 2 for a cylinder. For Sphere 1, the nominal strength value thus obtained was 55,000 psi. or 87.3% of the 63,000 psi. tensile strength of the control specimens. Since these are fictitious stress values in both cases, obtained under dissimilar types of loading, it can hardly be regarded as surprising that there is an apparent lack of correlation here.

Analogous to the true stress at ultimate load in a tensile test, the true stresses at ultimate pressure in the spheres were also calculated on the basis of the instantaneous dimensions of the shell as determined from external strain measurements at the time of maximum pressure. For Sphere 1, the true stress value thus obtained was 68,000 psi. as compared with 78,100 psi. for the true stress at ultimate load in the tensile control tests, the lack of correlation here being proportionally the same as it was for the nominal stresses.

In the sphere tests (Spheres 2 and 3 excepted) the pressure maximums occurred long before rupture, and were thus entirely a function of the resistance of the metal to plastic deformation. Since plastic deformation is caused only by shearing stresses, we may seek a better correlation on this basis. In the plastic range, a very logical assumption is that the principal shear stresses in the shell will be constant across the thickness (neglecting the small strain differential between the inner and outer surfaces). An equivalence of the shear stresses at maximum load in the sphere with that of the tensile test would then mean that the calculated (average) circumferential tension in the shell plus one-half the value of the ultimate pressure should equal the simple tension strength. That is, on a nominal stress basis

$$\sigma_{1u}{}' + \frac{p_u}{2} = 55000 + \frac{5900}{2} = 57950 \text{ psi.}$$

for Sphere 1, as compared with the 63,000 psi. tensile strength. This is a slight improvement, but on this basis we would still expect the nominal average tension in the shell to reach 95% of the tensile strength of the

material. On a true-shear stress basis, using the actual dimensions of the specimens, the correlation is less improved.

A better agreement between the calculated and observed strengths can be obtained in the present case on a nominal-shear stress basis by assuming that the tensile stress remains constant across the thickness while the shear stress has a maximum at the internal surface, and then requiring an equivalence of the shear stress on an element at the internal surface with that of the tensile test. This requires that we add the full value of the ultimate pressure to the calculated tension in the shell for comparison with the simple tension strength. On this basis we will obtain 55,000 + 5,900 = 60,900 psi. as the nominal effective stress in the shell, which is within 96% of the tensile strength of the control specimens; or, to put it the other way, the predicted nominal strength of Sphere 1 is then reduced to 63,000 − 5,900 = 57,100 psi., or about 91% of the tensile strength of the material. This assumption appears to furnish a reasonably good degree of correlation with the strength observed for the spherical shell test as well as for some of the other tests mentioned, but it is all the more at variance with the observations of Sachs and Lubahn[11] for the high-strength alloy steel tubes under internal pressure only, where the nominal bursting strengths were not lower, but higher than the tensile strength in simple tension.

The theoretical ultimate strength calculations in terms of the nominal stresses in the plastic range, it must be admitted, are but semirational procedures in which it is hoped that the effects of ignoring the dimensional changes and strain hardening of the metal at maximum load in one form of test will balance or cancel through a like disregard of the same factors in the tensile test. Apart from the effect of the biaxiality or the triaxiality of the stresses, it can be shown that this assumption is not generally valid for pressure vessels (nor for several other structural forms as well). On the other hand, the attempted correlations in terms of the true stresses based on the instantaneous dimensions of the specimens fell down in this case primarily because of the failure to take into account the fact that the internal pressure maximum is reached after a relatively small plastic extension in the sphere, when the state of the metal, as regards strain hardening and resistance to further deformation, is not comparable to that existing at maximum load in the tensile test.

A more rational theory of strength for the shell, which attempts to take these factors into account and which is in best agreement with the sphere test result, may be based on the plastic stress-strain relations given in Prof. Marin's report.[1] In the case of an idealized, isotropic material which obeys the generalized stress-strain relation given in Equation 1 above, it can be shown§§ that there is a definite functional relation between the nominal circumferential stress at ultimate pressure in a thin-walled shell (Equation 7), and the nominal tensile strength of the material. This relation is most easily expressed as a strength ratio:

$$\frac{\sigma_{1u}{}'}{\text{T.S.}} = \frac{\text{Nominal ultimate strength of shell}}{\text{Tensile strength of the material}}$$

$$= \frac{1}{\psi}\left[\frac{2\psi}{3(1-\beta)}\right]^n \qquad (8)$$

where ψ and β are the stress ratios defined by Equations (d) and (e) above, and n is the strain-hardening exponent. In the special case of a spherical shell, Equation 8 reduces to

$$\frac{\sigma_{1u}{}'}{\text{T.S.}} = \frac{1}{1-\beta}\,[^2/_3]^n \qquad (9)$$

Equation 8 is general in that it is theoretically applicable in the case of either a spherical or a cylindrical shell under various conditions of loading. The intermediate principle stress ratio ($\alpha = \sigma_2/\sigma_1$, where the maximum stress σ_1 is always in the circumferential direction) conceivably may have any value from zero (corresponding to the case of simple circumferential tension, e.g., as in an hydraulic standpipe) to unity, which will nearly always be known from the shape of the vessel and the condition of loading. The principal stress ratio (β) for the radial or thickness direction, is not strictly constant in value for a plastically deforming shell, but its value at maximum pressure is a function of the shell characteristics and is given by

$$\beta = \frac{-p_u}{2\sigma_{1u}} = \frac{-c}{2e^n}\left(\frac{t_0}{d_0}\right) \qquad (10)$$

where t_0/d_0 is the nominal thickness-to-diameter ratio, c is the geometrical factor of 4 for a sphere or 2 for a tube, and e is the base of the natural system of logarithms. It has been assumed that the deformation is homogeneous across the thickness, and, hence, that the thickness to diameter ratio is small (say $t_0/d_0 < 1/15$). The value of β is then normally small, and in the case of many thin-walled shells (say $t_0/d_0 < 1/30$ for a tube, or $t_0/d_0 < 1/50$ for a sphere) it probably can be neglected without very significant error. When β is negligibly small, Equation 8 may be written

$$\frac{\sigma_{1u}{}'}{\text{T.S.}} = \psi^{n-1}[^2/_3]^n \qquad (11)$$

where ψ is a function of α only.

Values of the theoretical strength ratio according to Equation 11 are shown in Fig. 5 as a function of α for several values of the strain-hardening exponent n. From this figure, it may be noted that the expectation that the nominal ultimate strength of a shell will be somewhat less than the tensile strength of the material does not represent the effect of the biaxiality of the stresses, but results from the mechanical instability associated with an internal fluid-pressure loading, and it is equally true for the case of simple tension hoop stresses calculated from a radial-pressure loading. In fact, it is the biaxiality of the stresses which, in accord-

§§ The derivation of Equations 8 to 11 has been omitted from the present discussion in order to conserve space. Readers interested in the mathematical basis of the equations may obtain an outline of the steps involved by writing directly to the author in care of the Bethlehem Steel Co., Bethlehem, Pa.

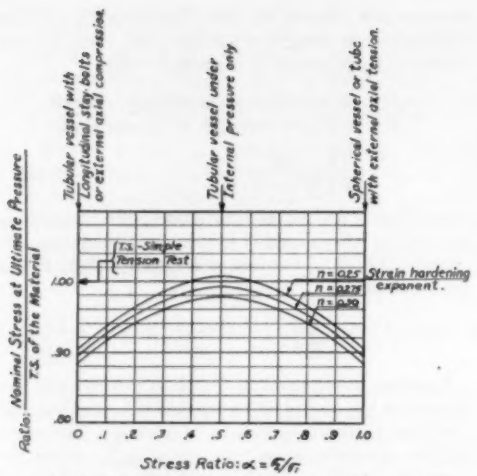

Tubular vessel with Longitudinal stay belts or external axial compression.
Tubular vessel under Internal pressure only
Spherical vessel or tube with external axial tension.
Ratio: Nominal Stress at Ultimate Pressure / T.S. of the Material
T.S. Simple Tension Test
n = 0.25
n = 0.27
n = 0.29
Strain hardening exponent.
1.00
.90
.80
0 .1 .2 .3 .4 .5 .6 .7 .8 .9 1.0
Stress Ratio: α = σ2/σ1

Fig. 5 Theoretical ratio of nominal ultimate circumferential stress due to internal pressure in thin-walled pressure vessels to the nominal tensile strength of the material when the wall thickness to diameter ratio is less than about 1:50.

ance with the shear strain energy¶¶ (or octahedral shear stress) formulations of the flow stresses, causes the central hump in the curves of this diagram.

For the semikilled steel used in Sphere 1, the reported[1] value of n was 0.27. Neglecting the influence of the pressure as a stress in the thickness direction ($\beta = 0$), from Equation 11 or Fig. 5, the predicted strength ratio for the test on Sphere 1 is found to be 0.896. With the value of β taken into account and using the theoretically more accurate Equations 8 or 9, the predicted ratio is 0.86. The experimental ratio observed for the spherical shell was 0.873.

The theory is general enough to account also for the case of the high-strength alloy-steel tubular specimens tested by Sachs and Lubahn[11] where the nominal ultimate-strength ratio was reported to be about 1.05. The actual value of n was not stated, but using an estimated value of $n = 0.10$ as about average for the high-strength level steel involved (240–270 ksi. tensile strength), it is interesting to note that the theoretical strength ratio given by Equation 8 for ideal conditions would be 1.05. A good correlation between the theory and the test results also appears to be possible in the case of the unwelded mild steel tubular specimens tested by Griffis, *et al.*,[3] despite the anisotropy of the material, provided the results are referred to the simple tension test on the tubular specimens as the basic tensile strength. These comparisons are shown in Table 4.

The allowable working stresses specified by the codes regulating the design of unfired pressure vessels for atmospheric temperature service are most commonly based on a factor of safety with respect to the tensile strength of the material. The nominal factor of safety assumed (usually 4, sometimes 5) is generally ample enough to allow for an approximate theory of ultimate strength in view of the other variables involved. It is not suggested, therefore, that the method of analysis described above would be in order for purposes of actual design calculations, but it does appear to provide a basis for a more rational appraisal of the assumed factors of safety for various conditions where questions of notch brittleness and joint efficiency do not control.

¶¶ If future experiments should indicate that some generalization of the plasticity condition[12] that gives a continuous function intermediate between the shear strain energy and the maximum shear stress conditions of yielding is more valid, the rise in these curves at intermediate values of α would tend to be less pronounced. The experimental data presently available, however, do not seem to warrant such a generalization or refinement at this time.

Table 4—Comparison of Theoretical and Experimental Ultimate Strength Ratios

Reference		*Marin*[1]	*Griffis*[3]		*Sachs*[11]
Type of test		Spherical shell	Small tubes		Small tubes
Material		Mild steel	Mild steel		High strength alloy steel
t_0/d_0		1/37.24	1/12		1/20
Value of n		0.27	0.31*		0.10†
Principal stress ratios	α	1.0	1.0	0.50	0.50
	β	−0.041	−0.061	−0.061	−0.045
Theoretical strength ratio Equation 8		0.86	0.83	0.92	1.05
Experimental strength ratio		0.873	0.84‡ 0.83§	0.93‡ 0.91§	1.05 (average)

* Calculated from published octahedral stress-strain curves.
† Estimated by means of reference.[13]
‡ Referred to simple longitudinal tension test on tubular specimen—as-rolled.
§ Referred to simple longitudinal tension test on tubular specimen—annealed.

SUMMARY AND CONCLUSIONS

The results of the tests on spherical shells under internal pressure, conducted at Pennsylvania State College, and other supplementary data and theory have been reviewed in an attempt to clarify their probable significance for engineering use. While the number of specimens tested has of necessity been somewhat limited, due to the expensive nature of such tests, the results obtained appear to support the following working conclusions:

1. Under biaxial tensile stress at room temperature, the (approximately 0.20 carbon) semikilled steels, for most practical purposes, may be said to retain about the same capacity for deformation and energy absorption as under conditions of uniaxial tension stress.

2. Under conditions of biaxial stress combined with subnormal temperatures down to −40° F., where the properties of the joints or other nonintrinsic notch effects are not permitted to control, the material still appears to retain a very substantial portion (probably greater than 60%) of its normal capacity for deformation and energy absorption in simple tension at room temperature.

3. Based on ductility or energy absorption criteria, the transition temperature to be associated with a "pure" biaxial state of stress apparently lies at subzero temperatures, considerably below that of the usual types

of notched-bar specimens. The transition temperature on a mode of fracture basis, however (i.e., the shear to cleavage transition), appears to lie within the range of normal service temperatures, and may be more or less comparable to that indicated by the slow notch-bend specimen or the Kahn Tear Test specimen. These comparisons are intended to be roughly correlative only.

4. A notch effect, such as may result from defective workmanship in a joint, can be the nucleus for initiating a very brittle type of failure in a biaxially stressed structure at low temperatures. Whether such a notch or flow is a greater potential hazard as a brittle crack starter than it would be in a uniaxially loaded plate of similar size has not been positively established, however. After the initiation of such a fracture, the tendency for it to become multiple-branching or a "shattering" type is probably more pronounced under biaxial tensions of equal magnitude.

5. The fact that the cleavage mode of fracture can be obtained within the probable range of service temperatures is a requisite but insufficient condition for poor load capacity of the structure, and for many purposes it may be economically impractical and unnecessary to attach too much importance to it. Whatever the source, critical notch effects at relatively low loads are essential to both poor ductility and unsatisfactory load capacity, and the key to the behavior will ordinarily lie in the joints (or in some attached appurtenance), as the most probable source of the necessary discontinuities.

6. With 100% joint efficiency and good ductility, the shell tested at room temperature developed a nominal ultimate strength of about 87% of the nominal tensile strength of the material. While comparative data are limited, it appears that this was probably a normal result for this type of vessel.

7. Lowering the testing temperatures caused the ultimate strengths of the shells to increase at a small but uniform rate in all cases except for two specimens in which premature brittle fractures originated in the joints.

8. Aside from matters of joint efficiency or other details of design, the nominal ultimate strength that can be developed in a thin-walled pressure vessel at normal temperature will usually be somewhat less than the nominal tensile strength of the material, and the ratio between them seems to be theoretically predictable to a good approximation, from the data obtained in a simple tension test by means of the equations given.

9. The theory of ductility under combined stress, with reference to the local ductility at the point of fracture, needs further substantiation but may find some limited application in the cold forming of sufficiently isotropic materials. It does not extend to the prediction of substantially brittle fracture in a normally ductile material.

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The Micro-Mechanism of Fracture in the Tension Impact Test

Discussion by W. B. Brooks

This paper is a classic, and one cannot compliment Prof. Bruckner and his associates too highly for the great effort, outstanding metallographic work, planning and thinking which obviously lie behind it. It is hoped that the author will describe the polishing techniques and optical equipment employed.

It is gratifying to have the sequence of twinning and fracture established, and it is especially gratifying to have this metallographic evidence of the now well-known difference in toughness between killed and rimmed or semikilled steels.

The writer would like to venture an explanation of the difference the author has reported in the microscopic behaviour of the two types of steels. Rather than killed vs. rimmed and semikilled steels, we may think of them as steels having weak and strong aging tendencies respectively. It is now widely held that plastic deformation occurs by the movement of dislocations through the lattice.[1] The more regular the lattice, the more easily dislocations can move through it. Conversely, if the lattice is distorted by the presence of larger or smaller solute atoms, or more especially precipitate particles bonded to the lattice,[2] the movement of dislocations is impeded. Thus, in the case of the weakly aging killed steels, local stress is relieved by the movement of dislocations, and the twins, if formed, do not propagate very far. Conversely, in the other type of steel, there is so much resistance to the movement of dislocations that local stress relief does not occur readily, and extensive twinning or fracture result.

It is hoped that we may have the privilege of more contributions of this type from Prof. Bruckner and his associates.

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Paper by W. H. Bruckner was published in the *Welding Research Supplement*, September 1950 issue of THE WELDING JOURNAL, pp. 467-s to 476-s.

Author's Reply

We are gratified to have the response which W. B. Brooks makes to the presentation of our paper. It is evident from his comments that he also is concerned about the reasons why the mechanism of fracture should be as reported in the paper. We are now extending the program in an endeavor to determine why the different steels have different twin nucleating and propagating characteristics. The explanation which Brooks suggests is attractive in dealing with the propagation of the twins and possibly also in understanding the reason for the nucleation of twins at ferrite grain boundaries. It would be expected that the grain boundary sites would represent the maximum concentration of impurity atoms and the maximum probability of initially exceeding the energy required to keep surrounding atoms in equilibrium lattice positions. Grain boundaries appear to be the source of the initiation of mechanical twins which are regarded by physicists as "half-dislocations." The half-dislocation is considered to be a more complicated movement in the lattice than is the dislocation itself. It will be important to know why this movement is generated in the disoriented material at grain boundaries and how to distribute the energy set free in this manner rather than permit it to concentrate along a cleavage crack.

The physicists speak of vacancies generated by the dislocations and the precipitation of impurity atoms from solid solution to the neighborhood of such dislocations. These events are of a much lower scale than we were concerned with initially in our metallographic survey but they must be considered if we are to determine whether or not cleavage failure is inevitable in the use of ferritic steels. Use of the electron microscope appears to be the most promising procedure in approaching the next lower scale of observation in fracture studies.

With regard to the polishing procedures, etc., used in examining the specimens for microfracture I will say that lead laps were used to obtain the largest possible area of polish. The first lead lap used was for 600 alundum followed by one for 303½ emery. Wet polishing with cloth using 305 emery and finally with magnesium oxide produced the desired surface. The last two steps were repeated as many times as necessary to obtain surfaces free from flow. In recording the microstructures filters were used to give maximum transmission of light in the blue-violet region which involved extremely careful and tedious focussing. Metallographic (orthochromatic) plates were used to record the details. The methods used are tedious but provide much better resolution than would have been possible with achromatic objectives and yellow-green light.

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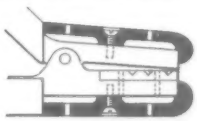

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